Role of mean stress on fatigue behavior of a 316L austenitic stainless steel in LWR and air environments

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ABSTRACT

Mean stress effects on fatigue life of a stainless steel in air and boiling water reactor (BWR) environments were evaluated by load-controlled tests. In stress-life, non-zero mean stresses increase life in low cycle fatigue regime in both environments and in high cycle regime in air. In high cycle fatigue regime in BWR environment, higher fatigue life was observed with negative mean stress while a decrease of the fatigue limit was found for positive one. In strain-life, the mean stress effects are insignificant. All data are well correlated with a Smith-Watson-Topper parameter showing no interaction between mean stress and BWR environment.

1. Introduction

Austenitic stainless steels (SSs) are used for pressure-boundary components (piping, cladding of vessels) in the primary reactor coolant circuit of light water reactors (LWR) [1], where they are exposed to high-temperature water and subjected to complex thermo-mechanical loading [2], potentially leading to fatigue or environmental-assisted fatigue (EAF). Primary pressure-boundary components are designed against fatigue and critical locations (e.g., nozzles, surge lines) are increasingly equipped with fatigue monitoring systems. Due to the soft operation with regard to fatigue and the very conservative design, fatigue damage in these components is very rare. Nevertheless, fatigue and EAF are potential concerns for the integrity and safety and thus have to be carefully evaluated for long-term operation of LWRs, in particular in the context of lifetime extensions beyond 50 years or of increasing flexible power operation (e.g., load following) in the future.

Fatigue design and evaluation of pressure-boundary components is usually done according to Section III, Division 1 of the ASME Boiler and Pressure Vessel Code (hereafter called ASME Code) [3]. The fatigue design curve in the ASME Code was derived mainly from strain-controlled low cycle fatigue (LCF) data with small, solid, smooth specimens tested in air at room temperature. These tests did not consider potential influence of LWR environmental factors explicitly. The design curves were initially derived from the mean fatigue curve in air by either applying a reducing factor of 2 on the strain range or a factor of 20 on fatigue life cycles, whichever gave more conservative results [4]. The reducing factors intended to cover the effects of material variability, surface finish, load sequence/history, size effects and atmosphere, which were not explicitly investigated in the underlying tests. The overall conservatism in the fatigue design curves is not only taken into account by the reducing factors but also results from the fatigue evaluation procedures, e.g., by the use of design transients that are significantly more severe than those experienced in service or simplified elastic-plastic analyses that result in higher stresses/strains.

Significant degradation effects of LWR coolant environment on fatigue lives in laboratory tests were observed in recent years [5–9] and there are no obvious reasons, why such effects should not occur in service, although their magnitudes might be significantly lower under most plant conditions. Under critical simulated LWR environmental conditions, i.e. when temperature, strain rate, dissolved oxygen (DO) level, and strain amplitude meet certain threshold values, the fatigue lives of material can be shorter than those in air [10].

Different Environmental Factors Approaches [4,6,11], such as US NRC Regulatory Guide 1.207/NUREG/CR-6909 and similar other national equivalents, were introduced to incorporate environmental effects into the usual ASME Code fatigue design and evaluation procedure. In this approach, the environmental reduction of the fatigue life is considered by a factor, the so-called $F_{en}$, which is equal to the ratio of the fatigue life in air at room temperature and of the fatigue life in LWR environment at operating temperature under otherwise identical conditions. In the $F_{en}$ approaches, only temperature, strain rate and dissolved oxygen are considered. The application of these procedures...
sometimes lead to high fatigue usage factors > 1 for critical components, and (eventually unnecessary) inspection efforts, although no fatigue damage was observed so far. These procedures are thus often regarded as unduly conservative. There is an apparent discrepancy between the excellent service record and laboratory investigations with strong environmental effects, which at least partially disappears, if the exact boundary conditions are carefully taken into account. There are significant differences between fatigue design, laboratory tests and the real component behavior. A careful industry-driven analysis of EAF evaluation procedures identified several practically important knowledge gaps in this context that were summarized in three comprehensive reports [12–14].

Actually, the thermo-mechanical loading and associated time history of real components are much more complex than the usual well-defined and simplified experimental testing conditions (isothermal, uniaxial, constant amplitude, zero mean-stress, polished surface, ...) selected for laboratory tests. Mean stress, non-proportional multiaxial loading, deformation/temperature history, water chemistry transients, surface roughness and conditions, strain amplitude (low cycle fatigue (LCF)) versus high cycle fatigue (HCF) or long static load hold time between the fatigue cycling are just examples of parameters with potential effects on EAF that are not sufficiently understood and that have to be investigated more in detail. To reach a high level of acceptance in nuclear industry, determining environmental correction factors for the above mentioned potential effects need massive and systematic non-standard testing, which is very challenging, costly and time consuming. Basically, such research goes well beyond single institution research capacity and international cooperation is necessary. New international efforts were initiated; for example, the INCEFA + project, started in mid-2015 within the European Commission Horizon-2020 program, was designed to deliver new experimental fatigue data to ultimately develop improved guideline in EAF [15–17]. Within INCEFA + project, three parameters were chosen, namely mean stress/stRAIN, hold time, and surface finish, to assess fatigue life sensitivity in light water reactor environment to these parameters.

In real conditions, mean stresses, originating from dead weight, internal pressure, residual stress, thermal gradient and stratification, exist in LWR components. However in most cases, mean stress effects on austenitic steels are investigated separately from the LWR environmental effects [18–23]. Only a small number of investigations were already undertaken to evaluate the mean stress and LWR environmental effects on fatigue of austenitic steels [7,24–27]. From load-controlled experiments Solomon et al. [27] studied the influence of a 100 MPa mean stress on the fatigue limit in air and pressurized water reactor (PWR) environment at 150 and 300 °C. The effect (increase or decrease of the fatigue limit) was found to depend on the temperature and the environmental temperature. This dependence was attributed to the amount of secondary hardening. Wire et al. also reported some tests carried out under load control and showed that the fatigue life increase with mean stress [26]. We emphasize that Solomon et al. and Wire et al. performed the load-controlled tests with relatively high mean stress, 100 MPas and higher, which is not very representative of the static stress existing in pressurized pipes. Indeed, it was shown by Spatig and Seifert [7] that static stresses that stem from the differential pressure are below 100 MPa.

For the time being, NUREG/CR-6909 [4] specifies that the Goodman mean stress correction are applied in deriving the revised air design curve for stainless steel but it does not specify the magnitude of stainless steel yield stress used to apply the correction; the issue is linked to the fact that austenitic steels do not exhibit a sharply defined yield stress. HCF and LCF may be sensitive to mean stress. In the EPRI report [12] it is clearly stated that: “The extent to which mean stress influences both low cycle fatigue and high cycle fatigue of stainless steel in air, and an appropriate means by which it should be accounted for without introducing undue conservatism, is considered to be a significant knowledge gap.”

This paper reports new fatigue data of a 316L stainless steel, obtained at 288 °C in air and simulated boiling water reactor/hydrogen (BWR/HWC) water chemistry, to gain insight into the unexplored effect of mean stress on the fatigue life. The influence of the environment (high temperature air versus high-temperature water BWR/HWC) was evaluated by load- and strain-controlled, isothermal, uniaxial fatigue tests with hollow cylindrical specimens. The present paper presents the set of data obtained and the mean stress and environmental effects and their synergetic interaction are discussed.

2. Material and experimental procedure

2.1. Investigated material

In this work, a 316L low-carbon austenitic SS was studied. A detailed description of this material can be found in [24] where preliminary results were already presented. For convenience, we just report here few details. The chemical composition is given in Table 1. The material was produced as hot finish hollow bar, 219.1 mm in outer diameter (OD), 19.5 mm in wall thickness (WT), 1300 mm in length by Sandvik (marketed as SANMAC 316L) in solution annealed (non-sensitized) and quenched condition.

The as-received material has a homogeneous texture-free, equiaxed austenitic structure with a mean grain size of about 60 μm and a high share of [24] twin grain boundaries of 38%.

2.2. Specimens preparation

Hollow specimens with outer diameter (OD) of 10 mm and wall thickness (WT) of 2.5 and 2.0 mm and a gage length of 18 mm were fabricated for fatigue tests in water and in air respectively. Only hollow specimens were tested in this study. Small tubes were welded to the drilled holes at the two heads of the hollow specimen to work as water inlet and outlet. The detail geometries are drawn in Fig. 1. All specimens had a 10 mm outer diameter and 18 mm gauge length. The wall thickness of the specimens tests in water was 2.5 mm while that of the specimen tested in air was 2.0 mm. Only few tests were performed in air with specimens having a wall thickness of 2.5 mm; those tests are presented in Fig. 8.

The surface finish of both inner and outer surfaces was controlled by the specimen manufacturer through honing. The surface roughness of the hollow specimen was characterized by the roughness values: Ra, Rsd and Rτ values defined as: Ra is the arithmetic average of the profile over the evaluation length; Rsd is the average of the successive values of Rt calculated over five sampling length, where Rτ is the vertical distance between the highest and lowest points of the profile within a sampling length within the evaluation length; and Rτ is the maximum height of the profile, namely the vertical distance between the highest and lowest points within the evaluation length. The measured inner and outer surface roughness values on the inner and outer surfaces are 0.41 μm of Rsa/3.57 μm of Rsd and 0.15 μm of Rsa/1 μm of Rsd respectively. The grinding scratches on the outer surface are parallel to loading axis; however, the scratches on the inner surface are tilted at ~ 45° to the loading axis. Fig. 2 shows two representative topographies of the outer and inner surface finish.

2.3. Fatigue test facilities and procedures

2.3.1. Fatigue tests in air condition

Fatigue tests in air were performed with a Schenck RMC 100 type electro-mechanical machine. High-temperature air environment was

<table>
<thead>
<tr>
<th>Chemical composition of the investigated austenitic stainless steel (in wt. %).</th>
</tr>
</thead>
<tbody>
<tr>
<td>Steel</td>
</tr>
<tr>
<td>316L</td>
</tr>
</tbody>
</table>

Table 1
provided by an ATS series 2961 oven, equipped with three heating zones and EUROTHERM2704 type thermal controllers. The difference between the set point temperature and the real temperature was smaller than \(\pm 2^\circ\text{C}\). The temperature variation along the gage was below \(\pm 3^\circ\text{C}\).

An in-house modified Epsilon model 3648–010 M025 ST extensometer (\(L_0 = 15\) mm, with \(\pm 2.5\) mm range, relative error within \(\pm 0.02\%\)) was used to measure the strain. The extensometer was cooled with liquid cooling by a Huber Minichiller 300, which also provided cooling for the upper and lower grips of the load train, which was equipped with a load cell with capacity of 50 kN.

The test conditions are summarized in Table 2. Load-controlled experiments were conducted with mean stresses of \(-20, 0, +50\) MPa. Other testing parameters, such as temperature, waveform, starting scenario, strain rate/frequency, were kept identical for tests in both environments. Fatigue life was defined when the measured elongation was \(>1.0\) mm (for load-controlled tests) and measured load drops 25% from the plateau level (for strain-controlled tests) or when the specimen totally broke into two parts.

### 2.3.2. Fatigue tests in water condition

Fatigue tests in water were performed with our in-house built fatigue test systems (Fig. 3), which consist of an Instron 8862 electro-mechanical machine and a water loop that can provide either BWR or PWR water chemistry conditions and allow thermo-mechanical loading. The detailed technical description can be found in [28,29]. The tests were performed in simulated boiling water reactor/hydrogen water chemistry (BWR/HWC) environment. The BWR/HWC environment is characterized by a temperature of 288 °C with a pressure of 100 or 200 bar, high-purity, deoxygenated (nitrogen purging) water with 150 ppb dissolved hydrogen. The water conductivity in the inlet and outlet was about 0.055 μS/cm and smaller than 0.070 μS/cm, respectively. The specimens were heated by the high-temperature water flowing through the hollow specimen with minimal axial and through-wall temperature gradients. The flow rate was 30 L/h resulting in a flow rate of 0.7 m/s in the specimen. A pre-oxidation period of 72 h was applied before starting the tests. During the pre-oxidation and test, a small compressive offset force was imposed to balance the tensile force induced by pressurized water. The strain signal was measured with an extensometer attached on the specimen outer surface. An AMTS 635.53F-30 type extensometer (\(L_0 = 15\) mm, with \(\pm 2.4/-1.2\) mm range, relative error smaller than \(0.5\%\)) was used for load-controlled tests and a Sandner Sensor EXA15-1u type extensometer (\(L_0 = 15\) mm, with \(\pm 1\) mm range, relative error within \(\pm 0.2\%\)) was used for strain-controlled tests. These were guaranteed by this extensometer, which in addition possesses a high eigen-frequency thanks to its short arms. Before starting the tests (both in air and water), the extensometer setup was checked (before and after heating) by loading one cycle with \(\sigma_a = 17\) MPa (namely force amplitude \(F_a = 1\) kN) to measure the elastic modulus. The measured modulus should be \(200 \pm 3\) GPa at RT and \(163 \pm 3\) GPa at 288 °C. The tests were started only if the measured Young’s modulus at RT and 300 °C were found within these limits.

Most fatigue tests were conducted in load-controlled mode but several in strain-controlled mode. Sinusoidal waveform (0.17 Hz before and 1 Hz after \(10^5\) cycles) was adopted for most tests. It has been
reported that the environmental reduction of fatigue initiation life in austenitic SS occurs when the strain rate $\leq \approx 7\%/s$, the temperature $\geq \approx 100 \degree C$ and the strain amplitude $\geq \approx 0.1\% - 0.15\%$, are simultaneously satisfied [30]. For the EAF study, the strain amplitude of interest ranges from 0.1% to 0.8%. Our load-controlled tests, carried out at 0.17 Hz, produce average strain rates 0.068%/s to 0.544%/s. (Note that the strain rate changes during each individual cycle as well as with cyclic hardening/softening and cycle number in the load-controlled tests). For tests running longer than $10^5$ cycles (of HCF), where the environment effect is moderate or absent, the frequency was increased to 1 Hz beyond $10^3$ cycles to shorten the test duration.

Positive mean stress (as high as +50 MPa) may lead to high plastic deformation (as high as 7–8%) if the maximum stress is reached in the first cycle [27]. For load-controlled tests, a specific starting scenario was implemented to minimize the initial tensile strain. A representative starting procedure of test under load-control with $\sigma_a = 210$ MPa and $\sigma_m = -20$ MPa in HTW is illustrated in Fig. 4. The tests were started by increasing the stress amplitude and mean stress incrementally and successively in tension and compression. For the tests without mean stress, the specimens were loaded according to the following scenario: first cycle starting in tension with $\sigma_a = 17$ MPa, second cycle starting in compression with $\sigma_a = 34$ MPa, third cycle starting in tension with $\sigma_a = $
51 MPa and so on, until the desired stress amplitude was reached. For the tests with mean stress, the stress amplitude was firstly set up as described above and then the mean stress was adjusted by successively increasing the absolute value of mean stress in 10 MPa increment each cycle, but in alternating the tensile and compressive direction, until the chosen mean stress level was reached. This procedure allows to work-harden the material without overstraining the specimens in either tensile or compressive direction. Tests under strain control were started by loading to maximum strain in the first cycle directly. During the tests, the mechanical loading parameters (including force, strain, travel displacement), the environmental parameters (including specimen temperature, heating water temperature, pressure, flow, dissolved hydrogen (DH), water conductivity) and water leakage and tank water level were monitored. The end of life was determined at the moment of leakage, no matter if the load or the strain was controlled.

3. Results

Fatigue test results in air at 288 °C are presented in Fig. 5 in the form of stress versus fatigue life. Only the results of the specimens with a wall thickness (WT) of 2.0 mm are shown. The most salient results show that both negative (-20 MPa) and positive (+50 MPa) mean stresses increase fatigue life, when \( N_f \leq 10^6 \) cycles, with respect to the tests with zero mean stress. However, for the tests with \( N_f > 10^6 \) cycles it seems that +50 MPa mean stress does not modify the fatigue life substantially and -20 MPa mean stress slightly increases the fatigue life and the limit at 10^6 cycles. We defined the region with \( N_f \leq 10^6 \) as the LCF regime and the region with \( N_f > 10^6 \) as the HCF regime. The data points at 10^6 cycles it seems that the HCF regime. The data points at 10^6 cycles it seems that

\[
\sigma_a = -20 \text{ MPa} : \quad \sigma_s = 510905 (N_f)^{-0.9} + 167 \tag{4}
\]

We introduce here a \( F_{\sigma_m} \) factor to quantify the effects of mean stress on fatigue life. Combining the Langer equations for zero and non-zero mean stress, we define:

\[
F_{\sigma_m} = \frac{N_f}{N_{f,\sigma_m=0}} = \left( \frac{B_{\sigma_m=0}}{\sigma_{\sigma_m=0} - \sigma_{f,\sigma_m=0}} \right)^{1/b_{\sigma_m=0}} \times \left( \frac{\sigma_{f,\sigma_m=0} - \sigma_{f,\sigma_m=0}}{B_{\sigma_m=0}} \right)^{1/b_{\sigma_m=0}} \tag{5}
\]

As the exponential constant \( b \) is fixed, we have then:

\[
F_{\sigma_m} = \frac{N_f}{N_{f,\sigma_m=0}} = \left( \frac{B_{\sigma_m=0}}{\sigma_{\sigma_m=0} - \sigma_{f,\sigma_m=0}} \right)^{1/b} \times \left( \frac{\sigma_{f,\sigma_m=0} - \sigma_{f,\sigma_m=0}}{B_{\sigma_m=0}} \right)^{1/b} \tag{6}
\]

Using the numerical values of Eq. (2), (3) and (4) and considering that \( \sigma_{f,\sigma_m=-20} \approx \sigma_{f,\sigma_m=+50} \approx 161 \text{ MPa}, \) and that \( \sigma_{f,\sigma_m=-20} = 167 \text{ MPa}, \) one gets \( F_{\sigma_m=-50} \approx 3.0 \) and \( F_{\sigma_m=-20} \approx 6.2 \) when \( \sigma_a \geq 180 \text{ MPa} \) (namely in LCF regime). Note that the fact that \( \sigma_{f,\sigma_m=-20} \) is not equal to \( \sigma_{f,\sigma_m=0}. \) Therefore Eq. (6) does not simplify to

\[
F_{\sigma_m} = \frac{N_f}{N_{f,\sigma_m=0}} = \left( \frac{B_{\sigma_m=0}}{\sigma_{\sigma_m=0} - \sigma_{f,\sigma_m=0}} \right)^{1/b} \tag{7}
\]

This shows a marked dependence on \( \sigma_a \) of \( F_{\sigma_m=\infty} \) factor near the fatigue limit.

For comparison purposes, it is interesting to plot all our fatigue life data (zero and non-zero mean stress) in terms of the cycle average strain. The data points at 10^6 cycles it seems that.
are well consistent with NUREG/CR-6909 predictions. In Fig. 6, positive mean stress data indicate a weak decrease in fatigue life with respect to zero mean stress data, while the opposite is observed for negative mean stress data. Generally speaking, when the fatigue life is represented in terms of the average strain amplitude, any difference induced by mean stress tends to vanish.

The strain evolution as a function of the cycle number was recorded and analyzed. Fig. 7 presents the strain evolution of the tests with \( \sigma_a = 190 \text{ MPa} \) and \( \sigma_a = 210 \text{ MPa} \) under different mean stresses. Evidently, the tests with different mean stresses but with the same stress amplitude start with different strain amplitudes due to the accumulated hardening at the end of the starting procedure (where +50 MPa mean stress hardens more than -20 MPa mean stress does). For all tests, the first hardening ends around 20 cycles. Then the softening stage takes over and continues to around 1000 cycles. Then a plateau appears, except for the tests with negative mean stress. In the case of negative mean stress, secondary hardening appears and lasts until the end of the fatigue life.

In Fig. 8, the stress-life results obtained in high-temperature air (HTA) with two specimen geometries are plotted together. The curves represent the best fits of results of specimens having a WT of 2.0 mm while the scatter corresponds to the results of specimens with WT = 2.5 mm. As can be seen, there is no notable difference in the results between the specimens with WT = 2.0 mm and WT = 2.5 mm in HTA, independently of the control mode and mean stress.

The stress-life (S-N) results of the tests carried out at 288 °C and 100 bar in BWR/HWC are plotted in Fig. 9 where only the results of specimens with a wall thickness of 2.5 mm are presented. Like for the tests in air, least squares fittings with Langer equation (Eq. (1)) were done:

\[
\sigma_m = 0 \text{ MPa}: \quad \sigma_a = 69447(N_f)^{-0.9} + 171 \quad \tag{11}
\]

\[
\sigma_m = 50 \text{ MPa}: \quad \sigma_a = 57477(N_f)^{-0.5} + 151 \quad \tag{12}
\]

\[
\sigma_m = -20 \text{ MPa}: \quad \sigma_a = 166336(N_f)^{-0.9} + 195 \quad \tag{13}
\]

Note that for the +50 MPa mean stress data, the exponent coefficient \( b \) in Eq. (1) had to be tuned to get a good fit quality. Keeping it equal to -0.9 yields indeed a poor fit through the data. Consistently with the observation of LCF tests in high temperature air, both positive (+50 MPa) and negative (-20 MPa) mean stresses increase fatigue life. In the HCF regime \( (N_f > 10^5 \text{ cycles}) \), -20 MPa mean stress increases fatigue life and fatigue limit (from 171 to 195 MPa), while +50 MPa mean stress decreases fatigue life and limit (from 171 to 151 MPa). Clearly in HTW environment, the mean stress effects on fatigue life are different from those in HTA environment. The synergistic effects of environments and mean stress will be addressed in detail in the discussion. The fitted curves of \( \sigma_m = 0 \text{ MPa} \) and \( \sigma_m = +50 \text{ MPa} \) intersect each other at around
\[ \ln \sigma_a = 6.907 - 1.538 \ln \varepsilon_a - 0.1 \]  

where \( \varepsilon_a \) is expressed in %, and \( T^* \), \( \dot{\varepsilon}^* \), \( O^* \) are dimensionless transformed parameters of temperature, strain rate and dissolved oxygen (DO) level respectively and are defined as:

\[ T^* = (T - 100) / 250 \]  
\[ \dot{\varepsilon}^* = \ln (\dot{\varepsilon} / 7) \]  
\[ O^* = \text{DO} \]  

\[ 0 \leq \dot{\varepsilon} \leq 7 \% / s \]  

In sine waveform loading, the strain rate is not constant. Thus the \( \dot{\varepsilon} \) in Eq. (17) is the average value over all cycles. In the Annex, a comparison between the more sophisticated modified rate approach (introduced in NUREG/CR-6909) and the average strain rate approach (applied in this study) for \( F_m \) calculation is done. It shows that the difference in \( F_m \) value is smaller than 15%.

In Fig. 10, our test results well agree with NUREG/CR-6909 predictions, except the section with very high strain amplitude. The NUREG/CR-6909 mean curve plotted based upon calculated data with Eq. (15) where the strain-considered depends on the strain-amplitude; we recall that the average strain-rate of our tests ranges from 0.068 to 0.544%/s because they are performed at constant frequency for different average strain amplitudes.

Two sets of strain amplitude versus cycle number are plotted in Fig. 11. These curves present the same characteristics as the strain evolution in Fig. 7 of the tests in HTA: all undergo cyclic hardening in the first 20 cycles, then a softening stage, which is followed by a plateau, except for the tests with negative mean stress. In Fig. 11a, we observed that the secondary hardening occurs after 1000 cycles for the tests with \( \sigma_m = +50 \text{ MPa} \) and \( \sigma_a = 190 \text{ MPa} \). Under these conditions, the specimen life was longer than 1000 cycles with small plastic strain and thus accumulated a significant amount of damage required for triggering secondary hardening.

4. Discussion

4.1. Mean stress effects on fatigue behavior

It is commonly recognized that positive mean stress is detrimental and negative mean stress is beneficial to fatigue life. This statement is generally true for materials cyclically loaded below the yield stress, or at least when very limited plastic deformation occurs during the cycles. This can be the case for the carbon steels and low alloy steels, whose fatigue limits are significantly lower than the yield stresses by at least 100 MPa [4,32], but not for austenitic stainless steels. For example, the 316L steel of this study has a yield stress around 150 MPa at 288 °C while the fatigue limit is around 160 MPa. If the mean stress is introduced with strain-controlled experiments by imposing a mean strain as it is the case in LCF experiment, the mean stress usually relaxes during the fatigue life due to a traction/compression asymmetry of the yield stress, and the mean stress reduction and rate of reduction depend on the amplitude of plastic strain component. This phenomenon of stress relaxation is observed only if a significant plastic strain occurs during the cycles, which is the case for the austenitic steels even when tested close to the fatigue limit but not for the carbon steels. If the mean stress is applied with load-controlled experiments, the initial cyclic hardening...
is reflected by a reduction of the plastic strain, and consequently to an increase of fatigue life. Thus, the observed beneficial mean stress effects on fatigue life is attributed to the enhanced cyclic hardening by mean stress, which results in smaller strain amplitude in comparison with zero-mean stress cases at a given stress amplitude. The plastic deformation per cycle is a key criterion to determine how many cycles the material can sustain. Figs. 12 and 13 describe the empirical relationships between the average strain amplitude and the stress amplitude with different mean stresses in air and BWR/HWC at 288 °C respectively. Both figures show that mean stress strengthens material during cyclic loading, which again is reflected by a decrease of average strain amplitude.

Interestingly, we found a linear relationship between average strain amplitude and stress amplitude, which depends on the mean stress. Furthermore, different slopes of the fits in air and BWR/HWC at 288 °C reveal an influence of the environment, mostly probably related to the internal water pressure, on cyclic mechanical behavior. The strain–stress relations are expressed mathematically as:

In 288 °C air:
\[
\varepsilon_a = A(\sigma_a - \sigma_m) + 0.1
\]  
(19)

In 288 °C BWR/HWC:
\[
\varepsilon_a = B(\sigma_a - \sigma_m) + 0.1
\]  
(20)

In Figs. 6 and 10, we see that the measured fatigue strain limit is around 0.1%. In Fig. 12, the fitted relationships have almost the same stress amplitude of \( \sigma_0 = 155 \text{ MPa} \) at the fatigue limit of \( \varepsilon_a = 0.1\% \). However, in Fig. 13 the fitted relationships have different \( \sigma_0 \) (160 MPa for \( \sigma_m = +50 \text{ MPa} \), 170 MPa for \( \sigma_m = 0 \text{ MPa} \) and 175 MPa for \( \sigma_m = -20 \text{ MPa} \)) at the fatigue limit of \( \varepsilon_a = 0.1\% \). This is consistent with our results in Figs. 5 and 9, where the tests with different mean stresses have practically the same fatigue stress limit in HTA, while they are different in HTW (151 MPa for \( \sigma_m = +50 \text{ MPa} \), 171 MPa for \( \sigma_m = 0 \text{ MPa} \) and 195 MPa for \( \sigma_m = -20 \text{ MPa} \)). In principle, mean stress should not affect much the average strain amplitude relationship at the fatigue limit (0.1%), where only small plastic strain is induced and where mean stress only slightly influences the material hardening. This is consistent with the cyclic stress–strain relationships in HTA of Fig. 12. However, different \( \sigma_m \) (higher for negative mean stress and lower for positive mean stress) were observed at \( \varepsilon_a = 0.1\% \) in BWR/HWC environment. Similarly,

### Table 3

<table>
<thead>
<tr>
<th>Mean stress [\text{MPa}]</th>
<th>Slope parameter A in HTA [\text{MPa}^{-1}]</th>
<th>Slope parameter B in HTW [\text{MPa}^{-1}]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>(8.97 \times 10^{-3})</td>
<td>(10.49 \times 10^{-3})</td>
</tr>
<tr>
<td>+50</td>
<td>(2.69 \times 10^{-3})</td>
<td>(4.26 \times 10^{-3})</td>
</tr>
<tr>
<td>−20</td>
<td>(3.16 \times 10^{-3})</td>
<td>(3.51 \times 10^{-3})</td>
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</table>

**Fig. 12.** Average strain amplitude dependence on stress amplitude with different mean stresses in air at 288 °C.

**Fig. 13.** Average strain amplitude dependence on stress amplitude with different mean stresses in 100 bar BWR/HWC environment at 288 °C.

**Fig. 14.** Average mean strain dependence on stress amplitude with different mean stresses in air at 288 °C.

**Fig. 15.** Average mean strain dependence on stress amplitude with different mean stresses in 100 bar BWR/HWC environment at 288 °C.
different fatigue stress limits were observed for tests with different mean stresses in BWR/HWC environment in Fig. 9. The synergistic effect between mean stress and BWR/HWC environment in HCF regime (discussed in Section 4.3) may be responsible for this.

For the fitted average strain amplitude-stress amplitude relationships in 288 °C air, the stress amplitude $\sigma_a$ at 0.1% strain amplitude is 155 MPa independent of mean stress. In 288 °C BWC/HWC, $\sigma_a$ depends on mean stress: $\sigma_a = 160$ MPa for $+50$ MPa mean stress, $\sigma_a = 170$ MPa without mean stress and $\sigma_a = 175$ MPa for $-20$ MPa mean stress. The A and B coefficients depend on mean stress in a non-linear manner and are presented in Table 3.

Mean stress under load-controlled condition induces mean strain, either in positive or in negative direction, as presented in Figs. 14 and 15. The evolution of mean strain during the fatigue life (ratcheting or cyclic creep) represents a directional progressive accumulation of plastic deformation of a material in the direction of mean stress [33]. Higher stress amplitude leads to larger accumulation of plastic strain in the mean stress direction per cycle for materials that deform elasto-plastically ($\epsilon_a > \approx 0.1\%$). This was experimentally verified: larger mean strains were found for the tests with larger stress amplitudes in Figs. 14 and 15. Mean strain was reported [18] to have minor effect on fatigue life under the same strain amplitude, but it can affect the crack density and opening (due to the larger maximum plastic strain). We observed that specimens with higher mean strain, which is positively related to mean stress, have a greater crack number density and opening on the inner surfaces [25]. The fitting lines in Figs. 14 and 15 intersect at $\tau_m = 0\%$. This shows that mean stress barely contributes to ratcheting (neither in tension nor in compression) in the HCF region.

4.2. Fatigue degradation in LWR environments

4.2.1. Stress-life representation analysis

The stress amplitude versus fatigue life (S-N) results with 0, $+50$, $-20$ MPa mean stress in both environments (air and BWR/HWC at 288 °C) are plotted in Figs. 16–18 respectively. Unlike the environment effect discussed in strain-life plots and quantified by the environmental factor independent of the strain amplitude $F_{\alpha} = N_{\alpha}^{\text{air}} / N_{\alpha}^{\text{BWR/HWC}}$, the environmental factor varies with the stress amplitude in the stress-life representation. Larger $N_{\alpha}^{\text{air}} / N_{\alpha}^{\text{BWR/HWC}}$ ratio is observed at higher stress amplitude. The fitting curves of the two environments intersect each other indicating that the BWR/HWC environment is detrimental only in the LCF regime. The BWR/HWC environment does not significantly change the fatigue limit with $+50$ MPa mean stress either. The BWR/HWC environment slightly increases the fatigue limit (from $\approx 161$ MPa to $\approx 171$ MPa) under zero mean stress condition and significantly increases the fatigue limit (from $\approx 167$ MPa to $\approx 195$ MPa) under $-20$ MPa mean stress condition. In the HCF region, LWR environmental effects vanish when the stress amplitude is below the threshold level. In Fig. 17, the plot with $+50$ MPa mean stress, the situation is still consistent with the conventional understanding. The beneficial effect of HTW in HCF regime in Fig. 16 and Fig. 18 could be tentatively attributed
to internal water pressure that induces a larger von Mises stress in compression than in tension [7] (< −7 MPa mean stress in our pressurized hollow specimen, which would increase the fatigue limit in HCF region, where the detrimental environmentally effect is negligible), or to oxide-induced crack closure effect under negative mean stress or to crack tip blunting by oxide layer. Corrosion can blunt crack tips resulting in lower driving force for crack growth. This could retard crack growth and contribute to fatigue life and limit increase. A more plausible explanation is the build-up of plasticity, roughness and oxide-induced crack closure effects, which can cause crack arrest [34].

\( N^{eff}/N^{BWR/HWC} \) dependence on stress amplitude was calculated for different mean stresses and plotted in Fig. 19, where the data points correspond to the experimental results and the curves are calculated from the best fit predictions. In the plot for \( \sigma_m = 0 \) MPa in Fig. 16, the fatigue life in air starts to outstrip the fatigue life in BWR/HWC around 190 MPa, where the corresponding average strain amplitude (\( \varepsilon_{amma} \)) is always > 1. For \( \sigma_m = -20 \) MPa, \( N^{eff}/N^{BWR/HWC} \) starts to outstrip \( N^{BWR/HWC} \) around \( \varepsilon_{amma} = 210 \) MPa. Above this stress amplitude, the HTW detrimental effects prevail resulting in shorter fatigue life in HTW; below this stress amplitude a longer fatigue life in HTW is found. The data points with \( N^{eff}/N^{BWR/HWC} > 1 \) in Fig. 18, both +50 MPa and −20 MPa mean stresses suggest similar environmental factors, which are larger than those without mean stress. This means that both positive and negative mean stress amplify the degradation effect of high-temperature water environment when considered in stress-life representation. The amplification of the environmental influence by positive mean stress was commonly recognized. However, the exact reason for the observed non-standard phenomenon of −20 MPa mean stress is unclear. The synergistic effects of environment and mean stress are addressed in detail in Section 4.3.

4.2.2. Strain-life representation analysis

As already mentioned, LWR environments significantly accelerate fatigue degradation when \( \varepsilon_{amma} > 0.112\% \), \( T > 100 ^\circ C \), \( f < 7\% / s \) and \( DO < 0.1 \) ppm [30]. The \( F_m \) factor (\( N^{eff}/N^{water} \)) concept was first adopted in JSME code to correct the environmental effects, which are not explicitly addressed in the ASME Code Section III [35]. Then NRC codes, ASME Code Cases and some European Codes adopted similar methodologies to evaluate the environmental effects on fatigue life. The \( F_m \) factor was reported to depend on temperature, strain-rate and dissolved oxygen content for austenitic stainless steels [4,6,11,35]. A dependence of \( F_m \) on strain range was reported by Kamaya based on the tested fatigue lives of 316L steel in PWR water environment [36]. He also observed that the environmental effect was insignificant at a strain amplitude of 0.25% and 0.22%. This is not consistent with JSME Code’s claim that there is no dependence of \( F_m \) factor on strain amplitude >0.11% and no environment effect when strain amplitude is equal or<0.11%.

All above mentioned statements are based on the results of strain-controlled tests with constant strain rate (normally saw tooth waveform was applied) and using solid specimens with smooth surface.

Thus, we use the average strain rate \( F_m \) approach in the following. We plotted the two best fits from Figs. 6 and 10 together in Fig. 20. The \( F_m \) factor, which was calculated from the two best fits (Eq. (9) and Eq. (14)) is independent of \( \varepsilon_{amma} \) and equal to 1.66. The \( F_m \) was also calculated with average strain rate approach (expressed in Eq. (A.37)). In this case, it varies from 1.66 to 2.4 due to the different strain rates in the strain amplitude range of 0.1% to 1.0%. The \( F_m \) factor of 1.66 calculated from the two best fits agrees reasonably well with the prediction from NUREG/CR 6909. The fatigue limit in strain is around 0.1% in both environments, showing no environment effect when \( \varepsilon_{amma} \leq 0.1\% \). This implies that environment as well as mean stress, which was discussed in last section, do not significantly change the fatigue limit in strain amplitude.

4.3. Synergistic effects of environments and mean stresses

Fatigue life consists of periods of crack nucleation and growth: short crack (microstructurally short crack in stage I/mode II + mechanically short crack in stage I/mode I) and long crack (stage II/mode I) as well as the unstable crack growth (stage III/mode I) in the last stage. The environment plays a different role in each period. Nucleation and short crack phases account for a larger portion of fatigue life at low stress/strain amplitude (high cycles regime) than at high stress/strain amplitude (low cycles regime), where long crack growth life is more dominant. Thus we discuss the environmental effect and mean stress effect on LCF and HCF separately.

LCF regime

We plotted the stress-life data in LCF regime of tests in both environments in Fig. 21, where only the data with 0 and +50 MPa mean stresses are reported. HTW decreases fatigue life with or without mean stress. +50 MPa mean stress increases fatigue life in HTW or in HTA. A first glance at Fig. 21 indicates that the environmental degradation (red arrows) is greater with +50 MPa mean stress than without mean stress and the corresponding beneficial factor (blue arrows) of +50 MPa mean stress is smaller in HTW than in HTA (compare...
For a more quantitative analysis, we defined the environmental factors, which are calculated from the stress-life data, as:

\[ F_{\text{en},0} = \frac{N_{0,\text{air}}}{N_{0,\text{water}}} \]

(21)

\[ F_{\text{en},50} = \frac{N_{50,\text{air}}}{N_{50,\text{water}}} \]

(22)

The environmental factors for \( \sigma_m = 0 \) MPa and \( \sigma_m = +50 \) MPa at different stress amplitudes are presented in Table 4, where the \( F_{\text{en}} \) are always > 1 and \( F_{\text{en},0}/F_{\text{en},50} \) ratios are always smaller than 1.

By combining Eq. (21) and Eq. (22), we get:

\[ \frac{N_{50,\text{water}}}{N_{0,\text{water}}} = \frac{N_{50,\text{air}}}{N_{0,\text{air}}} \times \frac{F_{\text{en},0}}{F_{\text{en},50}} \]

(23)

where \( F_{\text{en},0}/F_{\text{en},50} < 1 \). Thus,

\[ \frac{N_{50,\text{water}}}{N_{0,\text{water}}} < \frac{N_{50,\text{air}}}{N_{0,\text{air}}} \]

(24)

In other words, the beneficial factors of +50 MPa mean stress is smaller in HTW than those in HTA.

It has already been shown that positive mean stress enhances the environmental degradation of HTW during cyclic loading in stress-life representation [7,37,38]. Our results confirmed this observation and show negative correlation between \( \sigma_a \) and \( F_{\text{en},0}/F_{\text{en},50} \) are also seen in Fig. 19. Tests with positive mean stress lead obviously to a longer exposure time of the crack tip to HTW environment during the tensile loading. All possible HTW degradation mechanisms, which include the slip oxidation/dissolution and the hydrogen-induced cracking mechanisms, are favored by the loading condition with positive mean stress, or equivalently by higher maximum stress or maximum strain.

4.4. HCF regime

In the HCF regime (as Fig. 22 shows), tests with −20 MPa and 0 MPa mean stresses have a notably higher fatigue limit in HTW than in HTA. However, the fatigue limit difference at +50 MPa mean stress condition is insignificant and is still in the range of uncertainty in determining the fatigue limit.

The comparison between the fatigue limits in HTW (grey curve) and in HTA (red curve) shows that in HTW the fatigue limit is much more sensitive to the mean stress. From a mean stress of −20 MPa to +50 MPa, the fatigue limit decreases by ≈ 6 MPa in air while it drops by ≈ 43 MPa in BWR/HWC. All fatigue limits are attained at an average strain amplitude of ≈ 0.1%, as Figs. 6 and 10 show, at which the environmental effects disappear. This is true when \( \sigma_m \geq 0 \) but it is not necessary the case for the tests with \( \sigma_m < 0 \), when the oxide-induced crack closure effect is more pronounced. This is also reported by Zerbst [34] in studying compressive residual stress effects on crack closure of welds working in a corrosive environment.

4.5. Modified Smith-Watson-Topper mean stress correction model

The interpretation of the mean stress effect on fatigue life may appear somewhat contradictory depending on whether one considers the predictions based on the stress-life or on the average strain-life curves. Indeed, Eq. (24) suggests that a +50 MPa mean stress...
5. Summary and conclusions

1) Load-controlled fatigue tests with and without mean stress were carried out at 288 °C in air and in BWR/HWC environments. Enough data were obtained to highlight the similarities and differences of the mean stress effects in the two environments, and to assess the synergy between mean stress and environment effects. Load controlled fatigue data are usually reported in a stress-life plots (\(\sigma_m\) versus \(N_f\)). In such a representation, the mean stress effects can be summarized as:

- Negative mean stress is always beneficial for fatigue life and fatigue limit,
- Positive mean stress always increases fatigue life in the LCF regime (\(N_f < 10^5\)),
- In the HCF regime (\(N_f > 10^5\)), the fatigue limit (with positive mean stress) is practically identical to that with zero mean stress in air, but lower in BWR/HWC environment.

2) In BWR/HWC environment, we outline that:

- In the LCF regime, a decrease in fatigue life was systematically observed for a given mean stress in stress-life analysis. However, in the HCF regime, the fatigue life and fatigue limit do not seem to be significantly affected for \(\sigma_m = -50\) MPa, while a slight increase for \(\sigma_m = 0\) MPa and an obvious increase for \(\sigma_m = -20\) MPa is observed when compared with the fatigue data in air.

3) The effect of mean stress in fatigue life is intrinsically related to the life prediction model considered. We showed that the interaction between mean stress and LWR environment is detrimental for fatigue life when the predictions are based on plain stress-life behavior. However, life predictions related to the average strain-life behavior do not reveal an explicit interaction of mean stress with environment, as all the data with non-zero and zero mean stress are described by a single curve. This observation led to the conclusion that high quality fatigue predictions can be made provided that they are based upon one parameter characterizing simultaneously the strain amplitude and mean stress. This was further confirmed by verifying a modified Smith-Watson-Topper parameter, which has to be regarded as a generalized fatigue stress-strain relationship correlating all data with and without mean stress very well in a given environment. Finally, fatigue life predictions of real components with non-zero mean stress should rely on combined stress/strain functions like the average strain or the SWT parameter, which incorporates the mean stress effect in their definition. No synergistic effect between mean stress and environment was detected when using such parameters and the effect of LWR environment is consistent with the \(F_{en}\) calculated with NUREG/CR6909 equation.

Declaration of Competing Interest

The authors declare that they have no known competing financial
interests or personal relationships that could have appeared to influence the work reported in this paper.

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Appendix

In our case, we performed stress-controlled tests using sinusoidal waveform (Eq. (A.29), resulting in non-constant strain rate, as Fig. 25 shows. where $\omega = 2\pi f$ and $f = 0.17$ Hz is the frequency we applied for the sinusoidal waveform.

To estimate $F_{en}$ under these specific loading condition, we take the mid-life cycle (at the stabilized plateau) of one test with $\sigma_a = 210$ MPa, $\varepsilon_a = 0.425\%$ without mean stress as an example (in Fig. 25). As introduced in the experimental Section 2.3, the tests were conducted under load-control with a sinusoidal waveform at a frequency of 0.17 Hz. As Fig. 25 illustrates, the corresponding strain signal is neither a sinusoidal waveform nor a saw tooth waveform. It is not constant either, so for mathematical convenience we fitted the strain–time curve of the upload part with a power law:

$$\varepsilon(t) = \frac{\alpha}{n} t^n + B$$  
(A.30)

Through derivation of Eq. (A.30), the strain rate function is expressed as:

$$\dot{\varepsilon}(t) = \frac{\alpha}{n} t^{n-1}$$  
(A.31)

Based on the modified rate approach described in NUREG/CR-6909 Rev.1 [30], the $F_{en}$ for the total strain transient is given by:

$$F_{en} = \int_{t_{min}}^{t_{max}} F_{en}(T, \dot{\varepsilon}, O) \, dt = \frac{\int_{t_{min}}^{t_{max}} \exp(-T \dot{\varepsilon} O)}{\int_{t_{min}}^{t_{max}} \exp(-T \dot{\varepsilon} O)}$$  
(A.32)

$T$, $\dot{\varepsilon}$, $O$ are the transformed parameters of temperature, strain rate and DO level respectively. $T$ and $O$ are constant and calculated to be 0.752 and 0.29 respectively, based on Eq. (16) and Eq. (18).

Based on Eq. (17):

$$\dot{\varepsilon} = \ln\left(\frac{\dot{\varepsilon}}{\dot{\varepsilon}_0}\right) = \ln\left(\frac{\alpha t^{n-1}}{7}\right)$$  
(A.33)

Taking Eq. (A.34) and values of $T$ and $O$ into Eq. (A.33), get:

$$F_{en} = \frac{1.529_{0.2186}}{e_{max} - e_{min}} \sigma_{0.78192(n-1)+1}^{0.87912(n-1)+1}$$  
(A.34)

For the mid-life cycle in Fig. 25: $e_{max} = 0.425\%$, $e_{min} = -0.425\%$, $t_{max} = 2.9$ s, $t_{min} = 0$ s, the fitting parameters $\alpha = 0.08142$ and $n = 3.2778$ from Eq. (A.31), then we calculated the $F_{en} = 1.78$.

On the other hand, in the average strain rate $F_{en}$ calculation, we used average strain rate $\bar{\varepsilon}$, given by (t is the period):

$$\bar{\varepsilon} = \frac{\varepsilon_a}{t/4}$$  
(A.35)
\[ F_{\text{en}} = \exp\left(-T^2 \tilde{\epsilon} \mathbf{O}'\right) \] (A.37)

The average strain rate \( F_{\text{en}} \) is calculated to be 2.0. Comparing the calculated \( F_{\text{en}} \) factors with the modified rate approach and the average strain rate approach, we found a difference smaller than 15%.

References