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FATIGUE LIFE PREDICTION OF OIL DUCTS UNDER SERVICE LOADS

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Abstract

A methodology to calculate the residual initiation and propagation lives of fatigue cracks in oil pipelines with corrosion-like defects is proposed and applied to predict the residual life of an old duct made of API 5L Gr. B steel, in service for more than 40 years. Since its inauguration, this pipeline has carried several heated products under variable temperatures and pressures. The calculated (nominal) service stresses are very high, due to thermal loads that induce significant bending in curved parts of the duct, with peaks close to the yield strength of the steel. The elastic-plastic fatigue damage at a notch or a corrosion pit root is calculated using the εN method, and the effects of surface semi-elliptical cracks in its internal (or external) wall is studied considering appropriate stress intensity factor expressions and the actual service loads. In the presence of surface flaws associated to stress concentration factors of the order of three, a fatigue crack likely will initiate in the pipeline. However, if these surface cracks are small compared to the duct wall thickness, their predicted propagation rates are very low.

1. Introduction

Several steel pipelines in Brazil's have been in service since the 1960's, carrying heated oil products under variable temperatures and pressures. Mainly due to such thermal variations, which can induce significant bending cyclic service stresses in non-straight duct segments, metal fatigue must be considered in the calculation of their residual service lives. However, the initiation and propagation of fatigue cracks are usually not considered a major risk in the design of oil pipelines. The dominant failure mechanism in these cases is usually assumed to be corrosion, however a combination of both mechanisms must be considered when dealing with cyclic loads under such a long period of time. In this work, a methodology is proposed and applied to predict the fatigue crack initiation and propagation lives of an oil pipeline in service for more than 40 years.

2. Analytical Background

Fatigue is a type of mechanical failure caused primarily by the repeated application of variable

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loads, characterized by the gradual generation and/or propagation of a crack which can eventually cause the fracture of the structure. These phenomena are progressive, cumulative and localized.

The crack generation usually starts from a notch (which can be, e.g., a corrosion pit), and depends primarily on the range of the *local* stress ($\Delta\sigma$) or strain ($\Delta\varepsilon$) acting on the critical or most loaded point of the structure. For design purposes, $\Delta\sigma$ and $\Delta\varepsilon$ should be calculated on a volume large in comparison to the microstructural parameter which characterizes the material anisotropy (e.g., the grain size in metals). When the cyclic loads are large (causing macroscopic cyclic yielding), ductility is the main material fatigue strength controlling parameter.

When macroscopic cyclic yielding is present, the traditional method to design against fatigue crack initiation is the εN (Dowling, 1999; Stephens et al., 2001; Rice, 1988; Sandor, 1972). This is a *local* method, in the sense that its load history is completely described by the stress or strain acting at the critical point. In this manner, a strain gage and an appropriate stress concentration factor ($\mathbf{K_t}$) can provide all the loading information required to apply the εN design method.

The εN method correlates the number of cycles **N** to initiate a fatigue crack in any structure with the life (in cycles) of small specimens that should (i) have the same fatigue strength (hence, the same material and details) and (ii) be submitted to the same *strain* history that loads the structure critical point (generally a notch root) in service. Therefore, the εN and the SN methods are based in similar philosophies. As in the SN method, the εN method does not recognize the presence of cracks. However, the εN recognizes macroscopic elastic-plastic events at the notch roots and uses the local strain range (a more robust parameter to describe plastic effects) instead of the stress range to quantify them.

Neuber is the most used equation in the εN method to correlate the nominal stress $\Delta\sigma_n$ and strain $\Delta\varepsilon_n$ ranges with the stress $\Delta\sigma$ and strain $\Delta\varepsilon$ ranges they induce at a notch root. The Neuber equation states that the product between the stress concentration factor $\mathbf{K_\sigma}$ (defined as $\Delta\sigma/\Delta\sigma_n$) and the strain concentration factor $\mathbf{K_\varepsilon}$ (defined as $\Delta\varepsilon/\Delta\varepsilon_n$) is constant and equal to the square of the geometric stress concentration factor $\mathbf{K_t}$, thus

$$K_t^2 = \frac{\Delta\sigma \cdot \Delta\varepsilon}{\Delta\sigma_n \cdot \Delta\varepsilon_n} \tag{1}$$

When the nominal stresses are lower than $\mathbf{S_{Yc}}$, the cyclic yielding strength, it is common practice to model them as Hookean and, therefore, to use the Neuber equation in the simplified form

$$K_t^2 = \frac{\Delta\sigma \cdot \Delta\varepsilon \cdot E}{\Delta\sigma_n^2} \tag{2}$$

Ramberg-Osgood is one of many empirical relations that can be used to model the cyclic response of the materials. Its main limitation is not to recognize a purely elastic behavior, and its main advantage is its mathematical simplicity. It can be used to describe the stresses and strains at the notch root by

$$\Delta\varepsilon = \frac{\Delta\sigma}{E} + 2\left(\frac{\Delta\sigma}{2H_c}\right)^{1/hc} \tag{3}$$

where **E** is the Young's modulus, $\mathbf{H_c}$ is the hardening coefficient and $\mathbf{h_c}$ is the hardening exponent of the cyclically stabilized $\Delta\sigma\Delta\varepsilon$ curve.

Eliminating $\Delta\varepsilon$ from Equations (2) and (3), $\Delta\sigma_n$ is directly related to $\Delta\sigma$ by

$$K_t^2\,\Delta\sigma_n^2 = \Delta\sigma^2 + \frac{2E\Delta\sigma^{(h_c+1)/h_c}}{(2H_c)^{1/h_c}} \tag{4}$$

However, the above equation is logically incongruent, since it treats the same material by two different models: Ramberg-Osgood at the notch root and Hooke at the nominal region. Moreover, this procedure can generate significant numerical errors even when the nominal stresses are much lower than the material cyclic yielding strength. To avoid these type of errors induced by the classical Neuber approach, it is necessary to use the Ramberg-Osgood model to describe not only the stresses at the notch root, but also to describe the nominal stresses, writing

$$\frac{\Delta\varepsilon_n}{2} = \frac{\Delta\sigma_n}{2E} + \left(\frac{\Delta\sigma_n}{2H_c}\right)^{1/h_c} \tag{5}$$

In this case, given $\Delta\sigma_n$, the stress range at the notch root $\Delta\sigma$ can be calculated from Equations (1), (3), and (5), giving

$$K_t^2\left(\Delta\sigma_n^2 + \frac{2E\Delta\sigma_n^{(h_c+1)/h_c}}{(2H_c)^{1/h_c}}\right) = \Delta\sigma^2 + \frac{2E\Delta\sigma^{(h_c+1)/h_c}}{(2H_c)^{1/h_c}} \tag{6}$$

To quantitatively account for the errors induced by the Hookean modeling of the nominal stresses, a detailed study has been performed on measured properties of 517 different structural steels (Meggiolaro & Castro, 2001). It has been found that the Hookean modeling can lead to errors as high as 50% in the calculated stress at the notch root, especially if the nominal stress amplitude $\Delta\sigma_n/2$ is much above the cyclic yielding strength S_{Yc}. However, even if the nominal stresses are much smaller than S_{Yc}, the errors induced by the classical Neuber approach are very significant, reaching values up to 23% in some cases. And due to the non-linearities of the Coffin-Manson curve, these errors in stress translate to much higher non-conservative errors in life prediction. For instance, nominal stress amplitudes of only $0.3 \cdot S_{Yc}$ can lead in some materials to errors higher than 100% in life prediction, while values close to S_{Yc} may result in errors up to 2,000%. Depending on the considered material, even nominal stresses as low as $0.1 \cdot S_{Yc}$ can result in significant non-conservative errors. In summary, it is mandatory to use Ramberg-Osgood to model both the nominal and the critical stresses and strains, as shown in Equation (6), otherwise completely wrong crack initiation life predictions may be obtained.

On the other hand, the remaining life of cracked specimens can only be calculated using Fracture Mechanics concepts. The propagation rate of fatigue cracks da/dN is primarily controlled by the range of the stress intensity factor ΔK and **not** by the stress or strain ranges $\Delta\sigma$ or $\Delta\varepsilon$. The range ΔK depends not only on $\Delta\sigma$, but also on the crack length a and on the geometry of the cracked specimen, which can be written as

$$\Delta K = \Delta\sigma \cdot [\sqrt{(\pi a)}] \cdot [f(a/w)] \tag{7}$$

where $f(a/w)$ quantifies the effect of the geometric parameters that influence the stress field ahead of the crack front. Several $f(a/w)$ expressions can be found in the literature (Tada et al., 2000), in special for through cracks (which propagate in 1D).

However, fatigue cracks commonly nucleate as part-through cracks, which propagate in 2D. The main characteristic of these cracks is a non-homologous fatigue propagation: in general, the crack front tends to change form from cycle to cycle, because **ΔK** varies from point to point along the crack front.

Some analytical expressions are available for the stress intensity factor of 2D cracks, such as surface, corner or internal cracks under combined tension and bending. If the cracks have ellipsoidal fronts, and if they are built in a plate of width **w** or **2w** and thickness **t**, the stress intensity range **ΔK** is a function of the stress range **Δσ**, the ratios **a/c**, **a/t** and **c/w**, and an angle **θ** (Newman & Raju, 1984)

$$\Delta K = \Delta\sigma \cdot \sqrt{(\pi a)} \cdot f_\theta(a/c,\ a/t,\ c/w) \tag{8}$$

where **a** and **c** are the ellipsis semi-axes, f_θ is a crack shape function and **θ** is defined in Fig. 1.

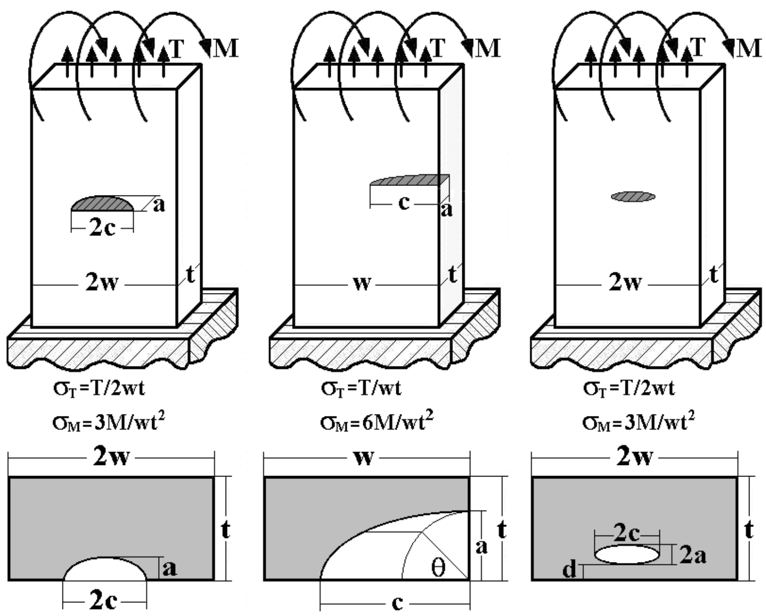

Figure 1. Surface semi-elliptical, corner quart-elliptical, and internal elliptical cracks.

The 2D ellipsoidal crack propagation problem is a reasonable approximation for many actual surface, corner, or internal cracks. Fractographic observations indicate that the successive fronts of those cracks tend to achieve an elliptical form, and to stay approximately elliptic during their fatigue propagation, even when the initial crack shape is far from an ellipsis (Castro et al., 1998). Therefore, it can be quite reasonable to assume in the modeling that fatigue propagation just changes the shape of the 2D cracks (given by the ratio **a/c** between the ellipsis semi-axes, which quantifies how elongated the cracks are), but preserves their basic ellipsoidal geometry. The idea is then to maintain the fundamental hypothesis of the ellipsoidal geometry preservation, accounting for the coupled growth in the depth (**a**) and surface width (**2c** for surface and **c** for corner cracks) directions. In the next sections, these methodologies are applied to calculate the residual life of oil ducts.

3. Material and Load History Data

The considered duct is made of API 5L Gr. B steel, in service for more than 40 years. Its mechanical properties were measured from test specimens removed from a section, resulting in the values shown in Table 1.

Mechanical property	Value
yield strength	$\mathbf{S_E = 294\ MPa}$
ultimate strength	$\mathbf{S_R = 423\ MPa}$
reduction in area	**RA = 60%**
Young modulus	**E = 208 GPa**
Cyclic hardening coefficient	$\mathbf{H_c = 1229\ MPa}$
Cyclic hardening exponent	$\mathbf{h_c = 0.24}$
Coffin-Manson's elastic coefficient	$\boldsymbol{\sigma_c} \mathbf{= 964\ MPa}$
Coffin-Manson's elastic exponent	**b = –0.145**
Coffin-Manson's plastic coefficient	$\boldsymbol{\varepsilon_c} \mathbf{= 0.36}$
Coffin-Manson's plastic exponent	**c = –0.55**
Crack propagation rate (m/cycle, **R=0.1**)	$\mathbf{da/dN = 2.1 \cdot 10^{-10}(\Delta K - 6.7)^{2.5}}$ ($\mathbf{\Delta K}$ in $\mathbf{MPa\sqrt{m}}$)
Propagation threshold (**R=0.1**)	$\mathbf{\Delta K_{th} = 6.7\ MPa\sqrt{m}}$.

Table 1. Mechanical properties of the API 5L Gr. B steel.

Since its inauguration, this oil duct has carried several heated products under operating temperatures between 60 and 80^{o}C, working under internal pressures between 6 and 15 kg/cm^2. A Finite Element (FE) analysis of the duct was performed considering both temperature and pressure effects, but neglecting any surface defect. It was found that the thermal bending stresses were about 8 times higher than the stresses induced by the internal pressure, therefore the temperature history was considered as the main input in the fatigue calculations.

It is found from the FE analysis that the nominal stresses (in **MPa**) due to thermal loads induce significant bending in the duct, with peaks close to the yield strength of the steel. Considering temperatures varying between 60 and 80^{o}C and a constant internal pressure of 15 kg/cm^2 (maximum peak in the history), the Mises stress history was then obtained:
$\{2\times[0\to217\to177\to217\to0] + [0\to257\to0] + 4\times[0\to217\to177\to217\to0]\}\times$ 12 months $\times$ 31 years (from 1961 until 1991) + $\{15\times[0\to177\to0]\}\times$ 12 months $\times$ 10,33 years (since 1992).

Even the largest peaks in the load history above must be considered as nominal service stresses, because they do not account for the effect of small surface flaws that could be present in the duct. In fact, these small defects are highly probable in a hot-rolled material operating for such a long time, and their effect must be considered in the analysis of the residual fatigue life of the duct. Thus, a stress concentration factor $\mathbf{K_t}$ has been postulated in the most stressed region of the duct, calculated assuming the effect of small semi-spherical corrosion pits in semi-infinite solids. Since semi-spherical notches have $\mathbf{K_t = 2.5}$ (Timoshenko & Goodier, 1970), it has been assumed in the calculations that $\mathbf{K_t}$ is between **2.0** and **3.0**. Note that it might be too conservative to assume $\mathbf{2.0 < K_t < 3.0}$ from the very beginning of the duct service life, when its surface was still intact (and therefore $\mathbf{K_t}$ was equal **1.0**). However, completely neglecting stress concentration effects could lead to unacceptable errors, since the probability of the occurrence of corrosion pits is very high. In addition, irregularly shaped pits may result in $\mathbf{K_t}$ values even higher than **3.0**.

4. Crack Initiation Predictions

Due to the very high nominal stresses, the SN method cannot be used, because it assumes elastic conditions at the critical point of the structure. Therefore, the elastic-plastic fatigue damage at a notch or a corrosion pit must be calculated using the εN method. However, the traditional εN formulation cannot be used when the nominal stress range is of the order of the yield strength, because in these cases Neuber's simplified equation can lead to non-conservative predictions, as discussed in Section 2. Therefore, elastic-plastic nominal stresses are included in Neuber's formulation to correctly account for plasticity effects both in the nominal region and at the most stressed point. Moreover, all the corrections necessary to warrant the prediction of physically admissible hysteresis loops at the notch root are included in the calculations, resulting in improved crack initiation predictions (Castro & Meggiolaro, 1999; Castro & Meggiolaro, 2003). These improved calculations were performed using **ViDa**, a powerful computer program developed to predict the crack initiation and propagation lives of structures and mechanical components under complex loading (Meggiolaro & Castro, 1998), see Tables 2 and 3.

Accumulated Damage since 1961				
K_t	Coffin-Manson	Morrow EL	**Morrow EP**	STW
1.0	0.005	0.007	**0.011**	0.037
2.0	0.077	0.092	**0.173**	0.340
2.5	0.175	0.202	**0.408**	0.681
3.0	0.334	0.378	**0.812**	1.194

Table 2. Fatigue damage predictions using the εN methodology according to the Coffin-Manson, Morrow Elastic, Morrow Elastoplastic and Smith-Topper-Watson equations (0.0 means no damage and 1.0 is associated with the initiation of a *small* fatigue crack).

Residual Life of the Oil Duct (in years before the initiation of a fatigue crack)				
K_t	Coffin-Manson	Morrow EL	**Morrow EP**	STW
1.0	> 100	> 100	> 100	> 100
2.0	> 100	> 100	> 100	> 100
2.5	> 100	> 100	**83.4**	22.3
3.0	> 100	76.6	**13.2**	-

Table 3. Residual life predictions for the oil duct against the initiation of a (small) fatigue crack.

Note that there is a significant variation in the predictions according to the several strain-life models, since each equation is more appropriate to a specific combination of material-loading conditions. These large differences explain the use of high safety factors (**SF ≥ 10**) in the SN methodology, which does not account for the plasticity effects at the notch root (DNV, 2000). In the case studied, since the mean plastic strain component is relatively high, probably the most accurate predictions are the ones obtained from the Morrow Elastoplastic equation. The other models are included for comparison purposes.

Note also that there is a high dependence between the notch stress concentration and the predicted damage. If the corrosion pits in the structure are all such that $\mathbf{K_t < 2.5}$, then no fatigue crack will be initiated for at least 100 years in service. In this case, fatigue may be neglected as a likely failure mechanism for this duct.

However, in the presence of surface defects of the order of $\mathbf{K_t = 2.5}$, the Morrow Elastoplastic equation predicts a residual life of 83.4 years, while Smith-Topper-Watson's (STW) method predicts (probably conservatively) 22.3 years.

In the case $\mathbf{K_t = 3.0}$, the residual life predicted by Morrow Elastoplastic is only 13.2 years, while STW would assume a small crack already present in the duct (damage greater than 1.0).

5. Crack Propagation Predictions

To evaluate the sensitivity of the duct to the presence of surface semi-elliptical cracks in its internal (or external) wall, crack propagation is studied considering appropriate stress intensity factor expressions and the actual service loads (see Fig. 2). Conservative estimates of the residual propagation life are obtained neglecting any crack retardation effect, such as crack closure.

Six configurations of 2D cracks were considered in this study: one semi-circular (with $\mathbf{c_i = a_i}$) and two semi-elliptical (with $\mathbf{c_i = 2a_i}$ and $\mathbf{c_i = 4 \cdot a_i}$), considering in each case the crack depths $\mathbf{a_i}$ = **2mm** and $\mathbf{a_i}$ = **4.25mm** (half the duct thickness). These cracks were assumed to be representative: the smaller ones are of the order of the smallest detectable defects associated with non-destructive techniques, and the larger ones are defects highly likely to be found during a careful inspection of the oil duct. The **ViDa** program was used in the calculations (Table 4), since it conveniently includes stress intensity factors for surface cracks at the inner wall of ducts, see Fig. 2. From these results, one must note that even the largest assumed crack cannot induce leakage in the duct, even had it been present since the beginning of its operational life. Such predictions, however, are highly sensitive to the assumed initial crack size.

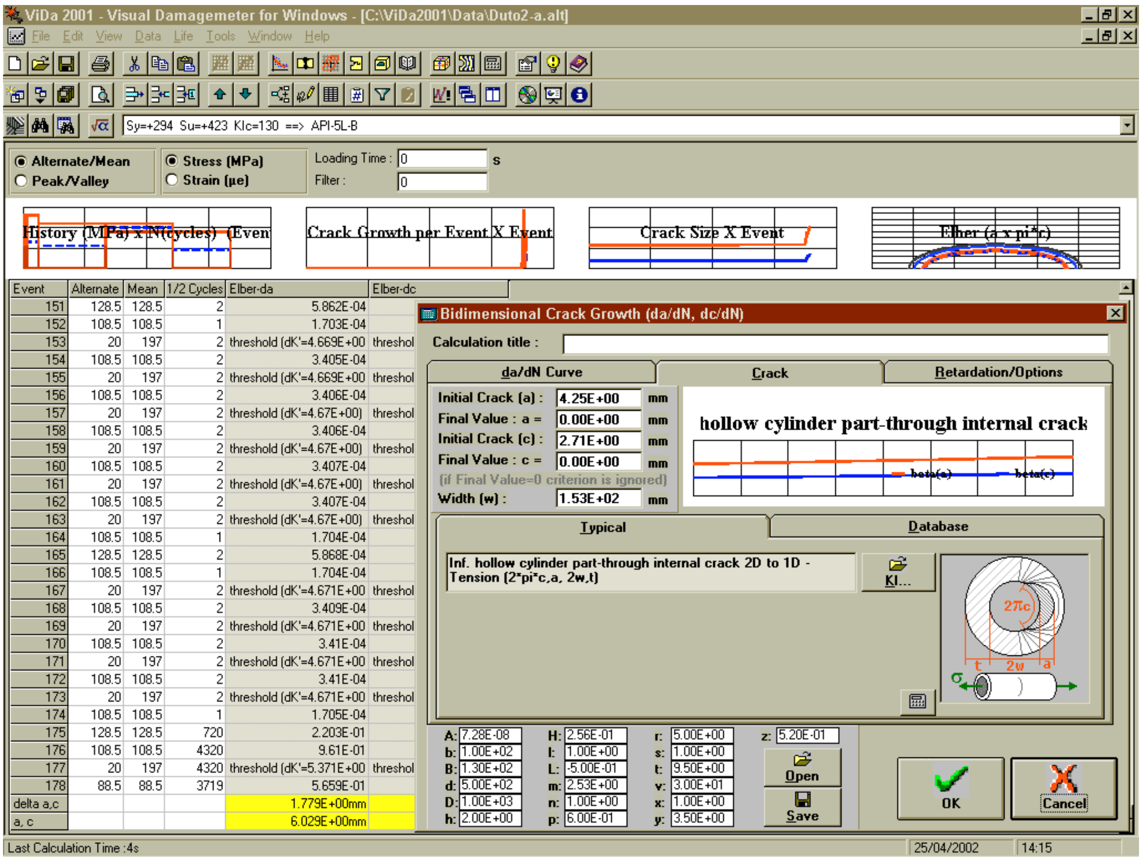

Figure 2 - Calculation results for surface crack growth in oil ducts.

Predicted Growth of Semi-Elliptical Surface Cracks with Depth a_i and Width $2c_i$ since 1961					
a_i (mm)	c_i (mm)	a_f (mm)	c_f (mm)	δa (mm)	δc (mm)
2.00	1.00	2.183	2.037	0.183	0.037
2.00	2.00	2.172	2.463	0.172	0.463
2.00	4.00	2.282	4.200	0.282	0.200
4.25	2.125	5.425	6.425	1.175	2.175
4.25	4.25	5.429	7.053	1.179	2.803
4.25	9.50	6.029	10.468	1.799	0.968

Table 4. Fatigue propagation of semi-circular and semi-elliptical surface cracks in the duct.

6. Conclusions

In the absence of notches or corrosion pits in the most stressed region of the duct, it is found that fatigue is unlikely to be an important failure mechanism. However, in the presence of surface flaws associated with stress concentration factors of the order of three, a fatigue crack is likely to be initiated in the duct. On the other hand, if these cracks are small compared to the duct wall thickness, its propagation rate is very low, becoming easily detectable before causing oil leaks to the environment or compromising its structural integrity.

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