Comparative study of multiaxial fatigue methods applied to welded joints in marine structures

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ABSTRACT

Marine structures are particularly prone to action of waves, winds and currents with stochastically varying composition, intensities and directions. Therefore, resultant stresses may cause multiaxial fatigue in specific welded structural details. For the assessment of multiaxial fatigue in welded joints, a wide variety of methods has been suggested. However, there is still no consensus on a method which can correctly account for non-proportional and variable amplitude loading. This paper beholds a comparative study of multiaxial fatigue methods applicable for design of marine structures. For the purpose of comparison several load cases were defined including non-proportional and variable amplitude load cases having different normal and shear stress amplitude ratios. Three types of methods are compared: those described by three different codes (i.e. Eurocode 3, IIW and DNV-GL), those described by three different critical plane based approaches from literature (i.e. Modified Carpinteri-Spagnoli Criterion, Modified Wohler Curve Method and Effective Equivalent Stress Hypothesis) and an approach based on Path-Dependent-Maximum-Range multiaxial cycle counting. From this study it has been concluded that non-proportional variable amplitude loading has a significant negative impact on the fatigue lifetime estimates, and that further research and experimental testing are essential to come to a consensus.

1 INTRODUCTION

Most welds in structural details of marine structures are predominantly subjected to uniaxial stresses due to the stiffness distributions in typical structural member assemblies like stiffened panels, frames and trusses. However, there are also welds which could be subjected to multiaxial stresses induced either by geometry (Maddox, 2010; Hong & Forte, 2014) or loading. Such stresses may lead to a significant reduction of the fatigue resistance of welded steel joints (Sonsino & Kueppers, 2001). Considering that the majority of marine structures are thin plated structures, such fatigue lifetime reductions are generally caused by the combined effect of a dominant normal and shear stress (mixed Mode-I and Mode-III) acting at the weld toe.

Currently, fatigue design of marine structures is predominantly based on uniaxial fatigue criteria assuming a governing Mode-I. These criteria are then used in combination with a damage accumulation hypothesis (e.g. Miner’s rule) and cycle counting method (e.g. rainflow counting) to determine the fatigue lifetime. However, such an approach can be non-conservative for structural details where the welds are subjected to multiaxial stresses, especially when these are non-proportional, i.e. out-of-phase (OP).

Over the last few decades intensive efforts have been made to develop multiaxial fatigue approaches which are able to deal with difficulties such as variable (VA) or even random (RA) amplitude loading and non-proportionality. This has resulted, amongst others, in multiaxial cycle counting methods (Wei et al., 2013; Anes et al., 2014; Meggiolaro et al., 2011), critical plane based criteria (Carpinteri et al., 2008; Susmel & Tovo, 2008; Li et al., 2011; Sonsino &
Kueppers, 2001), invariant based criteria (Cristofori et al., 2007) and energy based criteria (Macha & Sonsino, 1999). Furthermore, spectral methods have been developed to assess multiaxial fatigue in the frequency domain, instead of the time domain (Niestony, 2010). Despite all these efforts, still no consensus has been reached on an approach for the assessment of multiaxial fatigue in welded joints, whereby non-proportionality and variable amplitude loading can be accounted for correctly (Wang & Yao, 2004).

This study aims to identify the discrepancies resulting from the use of different multiaxial fatigue approaches for the fatigue analysis of welded joints in marine structures, considering load amplitude, proportionality and stress amplitude ratio. The comparative study has been carried out using several conceptual constant (CA) and variable amplitude load cases. Each CA case has been analysed using three different codes and three critical plane based multiaxial fatigue methods from literature. The VA cases have been analysed with an approach based on PDMR multiaxial cycle counting.

2 MULTIAXIAL FATIGUE METHODS IMPLEMENTED IN CODES

Considering codes which have been developed for the fatigue design of marine structures e.g. Eurocode 3, IIW, DNV-GL-0005, two types of approaches can be distinguished. They use either standardized interaction equations or the maximum principal stress (together with its relative direction with respect to the weld toe) (Hobbacher, 2008; Eurocode 3, 2005; DNV-GL, 2005).

2.1 Eurocode 3

For the fatigue design of steel structures, The European Union has established Eurocode-3. This code advises to account for the combined effect of the normal and shear stress components, acting respectively perpendicular and parallel to the weld toe, through an interaction equation (Equation 1). In this equation the constant amplitude equivalent normal stress $\Delta \sigma_{eq}$ and shear stress $\Delta \tau_{eq}$ are related to the design resistances $\Delta \sigma_R$ and $\Delta \tau_R$ defined at a certain number of stress cycles for a particular detail category.

$$\left( \frac{\Delta \sigma_{eq}}{\Delta \sigma_R} \right)^3 + \left( \frac{\Delta \tau_{eq}}{\Delta \tau_R} \right)^5 \leq 1 \quad (1)$$

2.2 IIW

Compared to Eurocode 3, The International Institute of Welding (IIW) has developed a more advanced interaction equation: Equation 2. The code allows for different material ductility (steel or aluminium), load characteristics (CA and VA loading, both in combination with either in-phase (IP) or OP loading), and a correction for fluctuating mean stress. For each particular case a critical Miner’s damage sum (equating to 0.2; 0.5 or 1) and comparison value $CV$ (0.5 or 1) is advised (Hobbacher, 2008). The design resistance of particular detail category is expressed by FAT class.

$$\left( \frac{\Delta \sigma_{eq}}{\Delta \sigma_R} \right)^2 + \left( \frac{\Delta \tau_{eq}}{\Delta \tau_R} \right)^2 \leq CV \quad (2)$$
2.3 **DNV-GL-RP-0005**

DNV-GL has established a recommended practice for fatigue design of offshore steel structures based on the maximum principal stress and its direction. The direction of the principal stress range is taken into account by an angle \( \phi \) between the maximum principal stress and the normal to the weld toe. However, once this angle exceeds a critical value, the weld toe may no longer be the critical location for fatigue crack growth. Therefore, a higher reference SN-curve is then recommended, depending on the detail category (DNV-GL, 2005).

3 **MULTIAXIAL FATIGUE METHODS FROM LITERATURE**

Multiaxial fatigue methods are often categorized on the basis of their approach, e.g. critical plane based, invariant based or energy based. Critical plane based methods originate from experimental observations where fatigue crack initiation (i.e. nucleation and early growth) appeared to occur on preferred material planes. However, in welded joints this crack initiation phase is affected by the welding process induced defects. On these grounds, three critical plane based methods have been selected to investigate their potential use on welded joints.

3.1 **Modified Carpinteri-Spagnoli Criterion**

Experimental observations demonstrated a correlation between the fatigue crack plane and the direction of the maximum principal stresses/strains and maximum shear stress/strain (Carpinteri et al., 1999). This led to the Modified Carpinteri-Spagnoli Criterion (MCSC) formulated as a quadratic combination of the maximum normal stress amplitude and shear stress amplitude acting on the critical plane (Carpinteri & Spagnoli, 2001):

\[
\left( \frac{\sigma_{\text{max}}}{\sigma_{A, -1}} \right)^2 + \left( \frac{\tau_{A}}{\tau_{A, -1}} \right)^2 \leq 1
\]

\( \sigma_{A, -1} = \text{fully reversed normal stress fatigue limit for bending (R = -1)} \)

\( \tau_{A, -1} = \text{fully reversed shear fatigue limit for torsion (R = -1)} \)

The shear stress amplitude acting on the critical plane can be determined in various manners but in this comparative study the Minimum Circumscribed Circle method was used. This method has been described in (Carpinteri et al., 2008; Carpinteri & Spagnoli, 2001; Papadopoulos, 1998).

3.2 **Modified Wohler Curve Method**

The Modified Wohler Curve Method (MWCM) accounts for the normal and shear stress components acting on a particular critical plane by incorporation of the maximum shear stress range \( \Delta \tau \) and stress amplitude ratio \( \rho = \Delta \sigma / \Delta \tau \). These two parameters are used in a linear relationship (see Equations 4-5) in order to construct a modified load specific shear stress based SN-curve defined by the offset parameter \( \Delta \tau_{\text{ref}} \) and slope \( m \) (Susmel et al., 2009).

\[
\Delta \tau_{\text{ref}}(\rho) = \left( \frac{\Delta \sigma}{2} - \Delta \tau \right) \cdot \rho + \Delta \tau
\]

\[
m(\rho) = (m_{\text{Mode I}} - m_{\text{Mode III}}) \cdot \rho + m_{\text{Mode III}}
\]
3.3 Effective Equivalent Stress Hypothesis

This hypothesis accounts for the interaction of shear stresses acting in different material planes based on the assumption that shear stress initiates multiaxial fatigue failure (Sonsino & Kueppers, 2001). The Von Mises equivalent stress is used in combination with an effective shear stress \( F(\delta) \) which is determined from the shear stress components (i.e. \( \tau_n \)) acting on each material plane:

\[
\sigma_{EESH}(\delta) = \sigma_{EESH}(\delta = 0) \cdot \frac{F(\delta)}{F(\delta=0)}
\]

\[
\sigma_{EESH}(\delta = 0) = \sqrt{\sigma_x^2 + \sigma_y^2 - \sigma_x \cdot \sigma_y + 3\tau_{xy}^2}
\]

The maximum local stress in the weld toe is considered governing for fatigue life and therefore the approach uses local stresses and requires a local reference SN curve.

4 COMPARATIVE STUDY

4.1 Constant amplitude loading

Five different conceptual CA load cases have been established presuming harmonic sinusoidal loading, see Table 1. The stress amplitude ratio was set to \( \sigma_A/\tau_A = 1/\sqrt{3} \) (with a normal stress amplitude of \( \sigma_A = 100 \text{ MPa} \)) and a frequency ratio of \( f_a/f_t = 1 \). Load case 5 is an exception whereby the frequency ratio is set to 2.

Reference SN-curves had to be selected for each code separately. For this purpose a non-load carrying fillet welded joint was presumed leading to CAT 80, FAT 80 and E Category for Eurocode 3, IIW and DNV-GL respectively. The reference SN curves were used to find the number of cycles \( N_f \) which meet the established criterion and were then transposed to fatigue damage using Miner’s rule (Exel & Sonsino, 2014). For LC 5 two different strategies have been applied. The first one is a conservative interpretation (referred to as LC 5.1), whereby the frequency of the normal stress component is presumed similar to the shear stress component (i.e. twice as high as actually is the case). The second strategy (referred to as LC 5.2) accounts the actual number of cycles of the shear stress component when finding agreement with the fatigue criterion. All damage sums have been normalized with the pure Mode-I load case (i.e. LC 1), for each code separately, and are listed in Table 2.

For the three selected critical plane based methods, particular reference SN-curves had to be used. For this purpose experimental data collected by Sonsino & Kueppers (2001) was used. Run-outs were excluded. The use of this data set is favourable as the stress concentration factors for bending and torsion of this test specimen are known. This enables to determine the local stresses at the weld which are needed for application of the EESH. To determine fatigue damage a reference SN-curve based on the local equivalent stress amplitude could now be used. This SN-curve was defined earlier by Sonsino & Kueppers (2001). For the MCSC and MWCM the pure Mode-I and pure Mode III curve were used (Susmel et al., 2009). All normalized damage sums are listed in Table 3. It should be emphasized that the results listed in Tables 2 and 3 show the relative differences between the different load cases.
Table 1: Definition of CA load cases

<table>
<thead>
<tr>
<th>Load case 1 (LC 1)</th>
<th>Load case 2 (LC 2)</th>
<th>Load case 3 (LC 3)</th>
<th>Load case 4 (LC 4)</th>
<th>Load case 5 (LC 5)</th>
</tr>
</thead>
<tbody>
<tr>
<td>- Pure tension -</td>
<td>- Pure torsion -</td>
<td>- Tension &amp; Torsion - In-phase</td>
<td>- Tension &amp; Torsion - Out-of-phase</td>
<td>- Tension &amp; Torsion - Out-of-phase</td>
</tr>
</tbody>
</table>

Table 2: Normalized effect of stress multiaxiality on fatigue damage predicted using selected codes

<table>
<thead>
<tr>
<th>Code</th>
<th>LC 1</th>
<th>LC 2</th>
<th>LC 3</th>
<th>LC 4</th>
<th>LC 5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Eurocode 3</td>
<td>1.0</td>
<td>0.41</td>
<td>1.4</td>
<td>1.4</td>
<td>2.8</td>
</tr>
<tr>
<td>IIW</td>
<td>1.0</td>
<td>0.41</td>
<td>2.6</td>
<td>9.8</td>
<td>20</td>
</tr>
<tr>
<td>DNV-GL-RP-0005(^1)</td>
<td>1.0</td>
<td>0.14</td>
<td>1.1</td>
<td>1.0</td>
<td>1.0</td>
</tr>
</tbody>
</table>

Table 3: Normalized effect of stress multiaxiality on fatigue damage predicted using selected critical plane based methods

<table>
<thead>
<tr>
<th>Critical plane method</th>
<th>LC 1</th>
<th>LC 2</th>
<th>LC 3</th>
<th>LC 4</th>
<th>LC 5</th>
</tr>
</thead>
<tbody>
<tr>
<td>MCSC</td>
<td>1.0</td>
<td>0.15</td>
<td>2.7</td>
<td>2.3</td>
<td>2.7</td>
</tr>
<tr>
<td>MWCM</td>
<td>1.0</td>
<td>0.15</td>
<td>1.2</td>
<td>1.3</td>
<td>1.3</td>
</tr>
<tr>
<td>EESH</td>
<td>1.0</td>
<td>0.02</td>
<td>1.7</td>
<td>2.5</td>
<td>4.4</td>
</tr>
</tbody>
</table>

4.2 Variable amplitude loading - Case study

In various previous studies on the applicability and validity of multiaxial fatigue methods conceptual load histories have been used (Anes et al., 2014a; Anes et al., 2014b; Mamiya et al., 2014). However, difficulties start to arise when it is intended to execute a multiaxial fatigue analysis on a structure under OP VA loading, which is representative for the actual day-to-day loading on marine structures. For this purpose a case study was developed by the authors and was then used to investigate the effect of stress amplitude ratio on fatigue damage using PDMR multiaxial cycle counting (Dong et al., 2009).

Simultaneous wind seas and swells generally dominate the wave spectrum of floating marine structures and therefore, the VA load case was defined as confused sea state consisting of wind driven seas and one swell. Wind driven seas were described by the mean JONSWAP spectrum as advised by the 17th ITTC in 1984 (Journee & Pinkster, 2002) and swell sea by a Gaussian swell spectrum (see Equations 8 and 9).

\[
S_{\text{JONSWAP}}(\omega) = \frac{320H_{swind}^2}{T_{p,\text{wind}}^4} \cdot \omega^{-5} \cdot \exp\left\{ -\frac{1950}{T_{p,\text{wind}}^4} \cdot \omega^{-4} \right\} \cdot \gamma^{
\text{\footnotesize{\(\text{}^{\text{\footnotesize{(8)}}}\)}}}
\]

\(^1\) Considering an in air environment
whereby \( A = \exp\left\{ -\left( \frac{\omega}{\omega_{p,\text{wind}} - 1}\right)^2 \right\} \); \( \omega_{p,\text{wind}} = \frac{2\pi}{T_{p,\text{wind}}} \); \( \sigma = \begin{cases} 0.07 & \text{if } \omega < \omega_p \\ 0.09 & \text{if } \omega > \omega_p \end{cases} \)

\[
S_{\text{Gaussian}}(\omega) = \left( \frac{H_{s,\text{swell}}/4}{(\lambda)^2} \right) \cdot \exp\left\{ \frac{-(\omega - \omega_{p,\text{swell}})^2}{2(\lambda)^2} \right\}
\]

This is in agreement with various guidelines for the marine industry (e.g. DNV-RP-C205; Kim et al., 2007). The directionality of the two spectra was fixed at a 180 degrees heading for wind seas and a 90 degrees heading for swell. It was assumed that wind driven and swell seas are long-crested and generate only normal and shear stresses, respectively. Additionally, it was assumed that stress spectral density functions are the same as wave spectra - meaning that unit response amplitude operators were assumed. Normally, multiplication of the wave spectra with response amplitude operators slightly shifts the spectra towards another frequency range. Therefore, it was necessary to choose the spectra parameters such that they correspond to a realistic structural response of a typical marine structure. The used spectra parameters are listed in Table 4 and the corresponding energy density spectra are depicted in Figure 1.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Description</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>( T_{p,\text{wind}} )</td>
<td>Peak period of wind seas</td>
<td>8 s</td>
</tr>
<tr>
<td>( T_{p,\text{swell}} )</td>
<td>Peak period of swell seas</td>
<td>14 s</td>
</tr>
<tr>
<td>( H_{s,\text{wind}} )</td>
<td>Significant wave height of wind seas</td>
<td>2 m</td>
</tr>
<tr>
<td>( H_{s,\text{swell}} )</td>
<td>Significant wave height of swell seas</td>
<td>2 m</td>
</tr>
<tr>
<td>( g )</td>
<td>Gravitational constant</td>
<td>9.81 m/s²</td>
</tr>
<tr>
<td>( \lambda )</td>
<td>Gaussian spectral width</td>
<td>0.02</td>
</tr>
<tr>
<td>( \gamma )</td>
<td>Peakedness factor of JONSWAP spectrum</td>
<td>3.3</td>
</tr>
</tbody>
</table>

The selected VA case is representative for a weld between a web frame bracket and the bottom plating, as shown in Figure 2. It is supposed that along the transverse weld over the bottom plating a multiaxial stress state can be generated consisting of a normal stress originating from global vertical bending of a ship in wind seas and a shear stress induced by web frame bending due to simultaneous swell. An illustration of such a local multiaxial stress state is depicted in Figure 2.

5 FATIGUE LIFETIME ESTIMATION

5.1 Multiaxial cycle counting

The intricacy in processing the generated time traces lies in the cycle counting procedure. The authors used their own multiaxial cycle counting algorithm developed based on publications of the PDMR cycle counting method (Dong et al., 2009; Wei & Dong, 2011; Wei & Dong, 2014). This algorithm was used to process time traces of normal and shear nominal stress which were generated with the two considered wave spectra. The generated time traces were scaled with four different stress amplitude ratios \( \left\{ \frac{T_A}{\sigma_A} = \frac{1}{1}; \frac{T_A}{\sigma_A} = \frac{1}{2}; \frac{T_A}{\sigma_A} = \frac{1}{3}; \frac{T_A}{\sigma_A} = \frac{1}{5} \right\} \) and then PDMR counted in \( \sigma - \sqrt{3} \tau \) stress space.
Figure 1: Energy density for wind seas and swell

5.2 Multiaxial damage accumulation

For quantitative comparison, the PDMR cycle counting results were converted into an accumulated damage and then normalized with respect to pure normal stress. For this purpose Miner’s linear damage accumulation rule was used in combination with a reference SN-curve. This reference SN-curve had to be compatible with PDMR cycle counting. Therefore, the selected experimental data from Sonsino & Kueppers (2001) was used again. For the four load cases (i.e. pure bending, pure torsion, combined IP loading and combined OP loading) the corresponding effective stress range was determined using PDMR cycle counting. In this case, these effective stress ranges correspond with the half-length of the load path in $\sigma - \sqrt{\tau}$ stress-space. Eventually, a mean SN-curve was establishing by making a linear regression as shown in Figure 3. Table 5 lists the parameters of the mean SN-curve. Accumulated fatigue damage was then calculated using the mean minus two times standard deviation SN-curve. Twenty minute time traces were used.

![Figure 2: Illustration of the local multiaxial stress components (normal and shear) with respect to the considered structural detail](image)

For comparison the CA load cases were also analysed using the PDMR based approach. The normalized results are listed in Table 7 and show that the virtual path length, which is identified in this counting procedure, has a large effect on the damage calculation. In Figure 4 a typical VA multiaxial load path is depicted. Figure 5 shows the histograms that resulted from PDMR cycle counting of this load path when the virtual path length is included and excluded. Again a significant impact of the virtual load path was observed. For twenty realizations the
average fatigue damage was calculated using PDMR cycle counting including and excluding the virtual path length. The values were normalized with pure bending and are listed in Table 6. Multiple realizations were needed because of unknown phases between the individual frequency components of the stress spectra.

Figure 4: Multiaxial load path of a twenty minute time trace from a three hour realization depicted in the $\sigma - \sqrt{3}\tau$ stress space whilst considering a stress amplitude ratio of $1$.

Figure 5: Histograms resulting from PDMR cycle counting of the load path depicted in Figure 4 (right); Virtual path included (left) and excluded (right).

Table 6: Average fatigue damage obtained with PDMR cycle counting for VA loading considering different stress amplitude ratios; Results normalized with pure bending

<table>
<thead>
<tr>
<th>Stress amplitude ratio $\tau_A/\sigma_A$</th>
<th>1:5</th>
<th>1:3</th>
<th>1:2</th>
<th>1:1</th>
</tr>
</thead>
<tbody>
<tr>
<td>$D_{avg}$ including virtual path</td>
<td>2.7</td>
<td>5.0</td>
<td>5.5</td>
<td>52</td>
</tr>
<tr>
<td>$D_{avg}$ excluding virtual path</td>
<td>1.5</td>
<td>2.3</td>
<td>2.1</td>
<td>25</td>
</tr>
</tbody>
</table>

Table 7: Fatigue damage obtained with PDMR cycle counting for CA load cases; Results normalized with pure bending

<table>
<thead>
<tr>
<th>PDMR based method</th>
<th>LC 1</th>
<th>LC 2</th>
<th>LC 3</th>
<th>LC 4</th>
<th>LC 5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Normalized damage</td>
<td>1.0</td>
<td>1.0</td>
<td>5.0</td>
<td>6.7</td>
<td>38</td>
</tr>
<tr>
<td>Normalized path length</td>
<td>1.0</td>
<td>1.0</td>
<td>1.4</td>
<td>1.5</td>
<td>2.7</td>
</tr>
<tr>
<td>Scaled maximum range</td>
<td>2.0</td>
<td>2.0</td>
<td>2.8</td>
<td>3.1</td>
<td>5.2</td>
</tr>
<tr>
<td>Number of half cycles</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>2</td>
<td>4</td>
</tr>
</tbody>
</table>

8
6 DISCUSSION AND CONCLUSION

From the three investigated codes it is found that Eurocode 3 does not distinguish between proportional (LC3) and non-proportional (LC4) loadings while IIW does. This is resulting from the use of different critical values in their fatigue criterion. Incorporating frequency induced non-proportionality using the conservative strategy (LC5.1), results in a doubling of the normalized damage obtained under phase shift induced non-proportionality. Interestingly, using the alternative strategy (i.e. LC5.2), Eurocode results in a higher damage, while IIW results in a lower damage than LC4. This is likely caused by the different damage mechanisms which are presumed (i.e. difference in power coefficients). A principal stress based approach, such as suggested by DNV-GL, results a slight reduction of fatigue damage under non-proportional loading in comparison to proportional loading due to a reduced maximum principal stress range. Furthermore, it does not distinguish between phase shift or frequency induced non-proportionality and due to the principal stress direction dependent reference SN-curve and Mode-I based slope, pure torsional loading results in a lower damage compared to Eurocode 3 or IIW. With IIW, non-proportionality has the highest impact on fatigue damage.

Looking at the results from the critical plane based methods, it appears that with the MCSC, the impact of non-proportionality on fatigue lifetime is less damaging or equally damaging to the proportional load case. However, this is in contradiction with experimental results of testing welded steel joints (Sonsino & Kueppers, 2001). Possibly the procedure which was selected to determine the normal and shear stress components acting on the critical plane should be changed. The MWCM is hardly capable to account for non-proportionality due to the fact that the stress amplitude ratio does not depend on the type of loading. From the three considered critical plane based methods the EESH results seem to be most affected by (non-)proportionality. However, this method becomes more complex when VA loading is under consideration.

In the PDMR based approach the virtual load path strongly affects the damage calculations. Moreover, the averaged normalized damage sums that were obtained in this study would require more realizations to achieve full convergence for stress amplitude ratio 1:3 and 1:2. This causes the averaged damage sum at a ratio of 1:3 to be slightly higher than at a ratio of 1:2 which seems contradictory. A non-linear relationship is observed between fatigue damage and the shear stress contribution (i.e. stress amplitude ratio). Analysing the constant amplitude load cases with the PDMR based approach showed that for non-proportional load cases the increased load path in combination with the reference SN-curve results in a significant increase in the normalized fatigue damage. Moreover, all comparisons in this study are based on nominal stresses. The use of more local stress information could therefore improve the results.

It can be concluded that there is no agreement between the fatigue damages calculated using the considered codes, critical plane based methods and the PDMR based approach. Overall, the proposed case study provides a basis for further investigation of multiaxial fatigue methods. However, particularly experimental testing under frequency induced non-proportional loading is expedient for validation, refinement or the development of new methods.

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