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APPLICATION OF NEW DAMAGE INDICATOR-BASED SEQUENTIAL LAW TO ESTIMATE FATIGUE LIFE OF OFFSHORE JACKET STRUCTURES

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Abstract. *The most of current fatigue life estimation methods of offshore jacket structures are based on Miner's rule. Miner's rule has always been acknowledged as a simplification that is easy to use in design where detailed loading history is unknown. However, under random amplitude loading, Miner´s rule might provide incorrect results because of its omission of the loading sequence effect. Recently, a new damage indicator- based sequential law has been proposed to capture the loading sequence effect more precisely than the Miner's rule. As offshore steel structures are subjected to variable amplitude loading and the state of the art methodology available allows us to measure or predict the detailed loading history, it is advisable to utilize the new damage indicator- based sequential law for offshore steel structures. Therefore, the objective of this study is to propose a sequential law employed new approach to estimate fatigue life of offshore steel jacket structures. The proposed approach consists mainly of the predicted stress histories, the recently developed sequential law and a technique for transferring the partially known S-N curve to a full range curve. Then the proposed approach is applied to predict fatigue life of an offshore jacket structure as a case study. The case study consists of 3D-modeling of a jacket platform, wave-structure interaction modeling, FEM-employed dynamic time history analysis, hot spot stress analysis, a technique for obtaining the full range S-N curve and fatigue life estimation based on the newly proposed sequential law. Finally, the proposed and the conventional approach´s predicted fatigue lives are compared to confirm the significance of the new approach.*

1 INTRODUCTION

The demand for exploration and production of oil and gas has grown ever since the early offshore activities began in the North Sea in the 1960's. The first steel structures to operate in the North Sea were transferred from the Gulf of Mexico, where exploration and production activities had been on going since the 1930's. Shortly after, it became clear that these structures were not adequate when operating in more severe weather conditions such as in the North Sea [1]. Early examples of fatigue damage are found in the "Sedco 135" triangular semi-submersible drilling rigs operating in the North Sea, as well as the jack-up drilling rig "Ranger 1" and the infamous "Alexander L. Kielland" platform – capsizing in March of 1980, leading to the loss of 212 personnel. Though the detail and quality of the fatigue design have improved since 1975, a significant amount of effort has been directed towards the development of structural health monitoring and non-destructive assessment methods of offshore steel structures in order to keep up with the gradual deterioration of strength due to crack growth, with the goal of applying design, inspection, maintenance and repair measures – thus ensuring adequate safety, as well as profitability with respect to using the same structures for further exploiting of the oil fields [2,3]. Therefore, it is essential highlighting the importance of understanding the deficiencies of currently available approaches, in order to progress and propose precise models to prevent similar kinds of future failures and even take measures for life extension.

The approach for fatigue design and fatigue assessment currently available is based on a combination of response data found by using the specific loading case (i.e. stress analysis and stress spectrum), material data describing the fatigue strength based on code provided S-N curves and a failure criteria which is based on the assumption of a cumulative damage commonly expressed by the Palmgren-Miner hypothesis (Miner`s rule). Further, if a crack is detected or the previous loading history is not known, fatigue strength is commonly based on fracture mechanics analysis (FMA). It is thus safe to say that the Palmgren-Miner rule has and still is accepted as the damage criteria for fatigue life estimation of offshore steel structures, and is acknowledged as an approved approximation that is easy to use in fatigue design where the detailed loading history (loading sequence) is unknown. However, research shows that under random amplitude loading, Miner´s rule might provide incorrect results because of its inability to precisely capture the loading sequence effect. Similarly, offshore steel structures are subjected to variable amplitude loading and the state of the art methodology available allows us to measure the detailed loading history to some extent. Recently, a new damage indicator- based sequential law has been proposed to capture the loading sequence more precisely [4]. The application of this model has not been studied for offshore steel structures. This is the question of concern in this paper.

Therefore, the objective of this study is to propose a sequential law employed new approach to estimate fatigue life of offshore steel structures. The proposed approach consists mainly of the predicted stress histories, the recently developed sequential law and a technique for transferring the partially known S-N curve to a full range S-N curve. Further, verification of the proposed approach is conducted by comparing the sequential law predicted damage behavior and corresponding fatigue life against the experimentally measured fatigue life and damage behavior of some materials respectively.

First, the paper presents the proposed approach for fatigue life estimation. Then verification of the proposed approach is presented by comparing experimental fatigue lives against theoretical predictions. Further, the proposed approach is applied to estimate the fatigue life of an offshore steel jacket structure and obtained fatigue life estimations are compared with the estimations obtained by the conventional theory of the Palmgren-Miner hypothesis. Finally the significance and applicability of the proposed approach is discussed.

2 PROPOSED APPROACH

This section presents the new proposed approach for fatigue life estimation of offshore steel structures. First, the general structural appraisal is presented before introducing the technique for obtaining the full range S-N curve, and the newly proposed damage indicator sequential law.

2.1 Wave-structure interaction and stress history evaluation

The following section presents the method for estimating the wave loads acting on the jacket legs, through the application of linear wave theory – where the final goal is determination of hot spot stress history of tubular joints.

Hydrodynamic loads are obtained on the basis of hydrostatics, hydrodynamics and linear wave theory, which is the core theory of ocean surface waves used in ocean and coastal engineering. This is the simplest wave theory, where wave height is considered to be much smaller than both the wave length and the water depth, and takes advantage of the linearized boundary conditions, where waves are considered as regular waves with sinusoidal shape, propagating with a permanent form. Linear wave theory is approved when assessing long period waves generated from distant storms (swell sea) [5]. Each wave has a distinct wavelength **λ**, wave period **T**, and wave height **H**. Application of linear wave theory requires that the following conditions be fulfilled:

- Continuity of mass – the net mass flow into an element equals the mass increase of the element.
- Based on the constant density of the fluid, the fluid is labeled incompressible.
- Another physical principle when assessing hydrodynamics is considering the water to be an ideal fluid where no shear forces occur between the particles (in other terms, consider the fluid to have a frictionless flow). Based on the assumption of non-rotational flow, the rotation of a water particle around its COG should be equal to zero.

Given that these conditions are present, a potential function **φ** exist such that the partial derivatives of this function with respect to the directions (x, y and z), give the velocities in each direction. If such a function exists, it is referred to as the velocity potential. Applying a set of boundary conditions set from physical principles (i.e. bottom condition, wall condition, kinematic surface condition and dynamical boundary condition), we are able to define the velocity potential as expressed in Eq. 1.

$$\varphi(x,z,t) = \frac{\xi_0 g coshk(z+d)}{\omega \quad \cosh(kd)}\cos(\omega t - kx) \tag{1}$$

Further, all types of offshore structures other than large floating bodies consist of slender cylinders – where a slender cylinder is defined as a cylinder of such geometry, which allows the diameter to be small in comparison with the wavelength (i.e. legs and braces of an offshore structure, subsea pipelines, umbilical cables etc.) Wave and current induced loads on slender members can be obtained from the Morrison`s load formula – consisting of an inertia force proportional to acceleration and a drag force proportional to the square of velocity [5]. The total force acting on the entire cylinder is as given as:

$$F(t) = F_1(t) + F_D(t) = \int_{-d}^{\xi} f_I\,(z,t)dz + \int_{-d}^{\xi} f_D(z,t)dz \tag{2}$$

where

$$f_I = \rho.(1 + C_A).A.\dot{u}$$
$$f_D = \frac{1}{2}.\rho.C_D.A.u.|u|$$

where C_A and C_D are the mass- and drag coefficients respectively. These hydrodynamic coefficients are based on experimental data and the relation between these coefficients and the governing parameters are as follows:

$$C_A = C_A(R_{e,}K_c, \Delta)$$
$$C_D = C_D(R_{e,}K_c, \Delta)$$

The drag term obtained from the Morrison´s load formula is quadratic and 90° out of phase with respect to the inertia term. One tries to avoid using the complete Morrison equation unless it is absolutely necessary. A simple way of doing this is by studying the Keulegan-Carpenter (K_c), which is a very important parameter for determining the relative importance of drag versus inertia forces for the case under consideration. Based on this one can determine whether the drag or inertia force is negligible, whether the drag force can be linearized, or whether one must use the full Morrison`s equation with its nonlinear drag.

In general, given the linear wave theory extrapolation and its corresponding assumptions, it is further assumed that the wave-induced motions are of the harmonic nature as well, and can be expressed by a sinusoidal function as in Eq. 3.

$$u(t) = u_o \sin(\omega t) \quad (3)$$

In general, it follows that the dynamic equilibrium equation for a fixed structural member can be written as:

$$m\ddot{r} + c\dot{r} + kr = F(t)$$
$$m\ddot{r} + c\dot{r} + kr = F_I(t) + \frac{1}{2}.\rho.C_D.D.\Delta I.u_0^2.sin\omega t.|sin\omega t| \quad (4)$$

Notice that the drag term is neither proportional to the wave amplitude, nor harmonic. Linearization is required given a situation where the drag term can`t be neglected. Previous research shows that linearization can be given by a constant $\frac{8}{3\pi}$ times an unknown parameter *A* [6,7]:

$$u_w(t) = u_a cos\omega t \quad (5)$$
$$A = \sqrt{(u_a - \omega.r_2)^2 + \omega^2.r_1^2}$$

Where u_a is the amplitude of a regular wave`s velocity function, and r_1 and r_2 are the sine and cosine response components, respectively. It follows that for a system with small response amplitudes, the damping term from the drag forces can be neglected ($r_1 = r_1 = 0$ and $= u_a$. This gives the following linearized dynamic equilibrium equation:

$$m\ddot{r} + c\dot{r} + kr = F_I(t) + \frac{1}{2}.\rho.C_D.D.\Delta I.\frac{8}{3\pi}.u_a^2.cos\omega t \quad (6)$$

Should the structural response amplitude become significant, one shall take account for the relative velocity between the structural member and the water.

A finite element employed time history analysis is conducted with the goal of obtaining the nominal stress histories of the most critical members. Wave loads described by time history functions are utilized as input load time history. Fatigue analysis based on S-N data is typically related to a nominal- or hot spot stress approach. When assessing other types of structural details (i.e. welding details), the nominal stress range should be modified in order to take account for the local conditions affecting the stresses at a specific location. The local stress at this location is expressed by a stress concentration factor multiplied with the nominal stress (Eq. 7). It is most common that the stress concentration factor results into an amplification of the nominal stress. However, there are cases where a stress concentration factor less than 1 can validly exist.

$$\sigma_{local} = SCF.\sigma_{nominal} \quad (7)$$

Stress concentration factors for different frame elements and the different loading conditions are to be calculated in reference with recommended practice [8], where a huge variety of equations are presented – defining stress concentration factors for different types of joints under different loading conditions.

Further, nominal stress history output is used for calculating and identifying the hot spot stress at the crown and saddle points. Furthermore, the hot spot of each structural component is to be calculated around the circumference of the intersection (Fig. 2), where the highest value obtained defines the hot spot. Further analysis and fatigue life estimation of the structural component is based on the corresponding hot spot. Fatigue life estimations of the tubular members, which are to be presented in Section 4 are based on T-curves with cathodic protection.

The hot spot of the chord member is to be assessed at three different locations (i.e. brace-chord intersections with the left hand side brace, middle brace and the right hand side brace – as sketched in Fig. 1). This means that when assessing the chord, one ends up with having to evaluate the stresses at eight different points (as shown in Fig. 2), for each of the three intersection locations (Fig. 1) – where the highest value obtained identifies the hot spot for the chord. Else, when assessing each brace, one only looks at the specific brace-chord intersection place of the corresponding brace. Note that each "stress point" represents its unique stress-history function.

2.2 Fully known S-N curve

S-N curves are in general obtained from fatigue tests of specimen mainly subjected to axial and bending loads. The basic design S-N curve can hence be expressed as follows.

$$\log N = log\bar{a} - mlog\Delta\sigma \quad (8)$$

However, the regular S-N curve only describes the stress ranges corresponding only to tens of thousands of failure cycles (i.e. only describes the high cycle fatigue region), hence they are often labeled as partially known S-N curves [9]. Extending the partially known S-N curve to a full range curve is therefore of the essence when assessing fatigue based on the damage-indicator based sequential law. The method for this transformation is mainly based on Kohout and Vechet Wöhler curve modeling technique [10]. A schematic overview of this technique is presented in Fig. 3.

Where the horizontal line 1 along the ultimate tensile strength is the asymptote $s = s_u$ for the low cycle fatigue region. The horizontal line 2 represents the stress range for the high cycle fatigue region (where $s = s_{¥}$), while line 3 represents the tangent for the region of finite life described by equation (or curve) of the partially known S-N curve provided by the design code. Location B and C show the intersection points of the tangent line 3 with the horizontal lines 1 and 2. The full range curve is at any given point expressed by the following expression [9, 10].

$$\sigma = \sigma_{\infty}\left(\frac{N+B}{N+C}\right)^{b} \quad (9)$$

where b is the slope of the tangent.

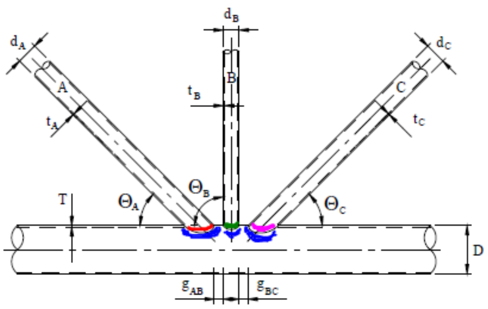

Fig. 1:Arbitrary KT-joint [8]

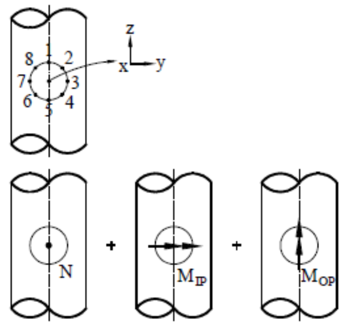

Fig. 2: Hot spot around the circumference of the intersection [8]

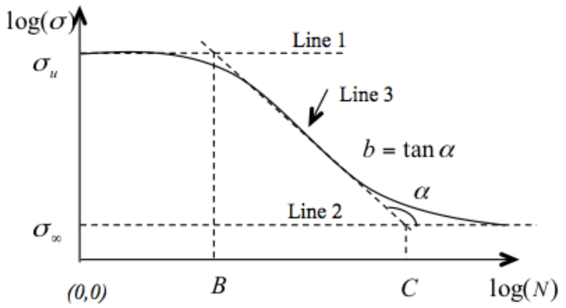

Fig. 3: Schematic presentation of the full range S-N curve modeling technique

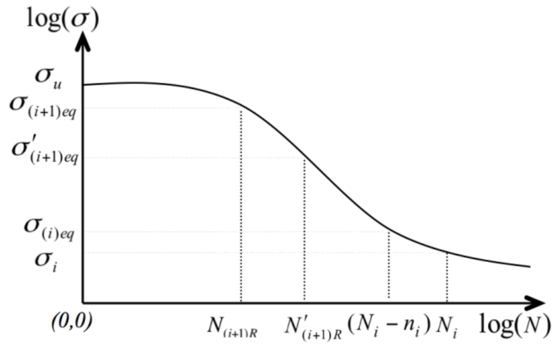

Fig. 5: Schematic representation of the sequential law parameters versus the S-N curve

2.3 Damage indicator based-sequential law

The modern day technology allows us to measure the loading history on most of the existing civil structures, whether they are onshore or at sea. The sequential law provides an algorithm for properly assessing the fatigue cumulative model – especially under variable loading conditions, and was developed for the purpose of capturing the loading sequence effect more precisely [4,10].

A new damage indicator based sequential law is used to obtain more realistic fatigue life estimation of offshore steel structures. The sequential law provides an algorithm for properly assessing the fatigue cumulative model – especially under variable loading conditions [4]. The supposition of this fatigue criterion is that if the physical state of damage is the same – then fatigue life depend on the loading condition only..

A detailed description of the damage stress model and the definition of damage indicator, D_i, is described in the corresponding paper [9], [10]. Here within, only the concept is summarized with an algorithm for comprehension (see flow chart presentation in Fig. 4). Suppose a structural component is subjected to an arbitrary stress amplitude or stress range s_i for n_i number of cycles at load level i, where N_i denotes the fatigue life corresponding to s_i. The residual fatigue life at load level i is obtained from N_i - n_i. The equivalent stress amplitude or stress range corresponding to the residual fatigue life is denoted $s_{(i)eq}$, and is equal to s_i for the first cycle.

Further, the new damage indicator, D_i, is then expressed by Eq. 10. It also follows that the damage indicator is equal to zero at the first cycle given the fact that $s_{(i)eq} = s_i$ at the first cycle.

$$D_i = \frac{\sigma_{(i)eq} - \sigma_i}{\sigma_u - \sigma_i} \tag{10}$$

Where s_u is defined by the intercept of the S-N curve with the ordinate at one-quarter of the first fatigue cycle [9,10]. Furthermore, s_u is also commonly known as the ultimate tensile

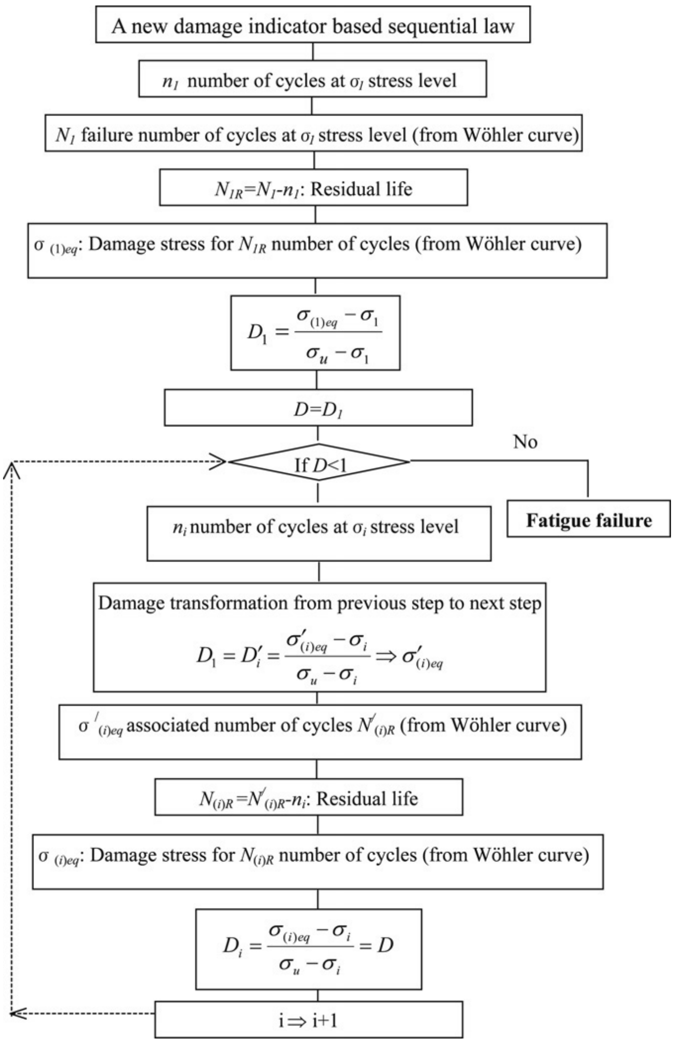

Fig. 4: Flow chart for the damage indicator based sequential law

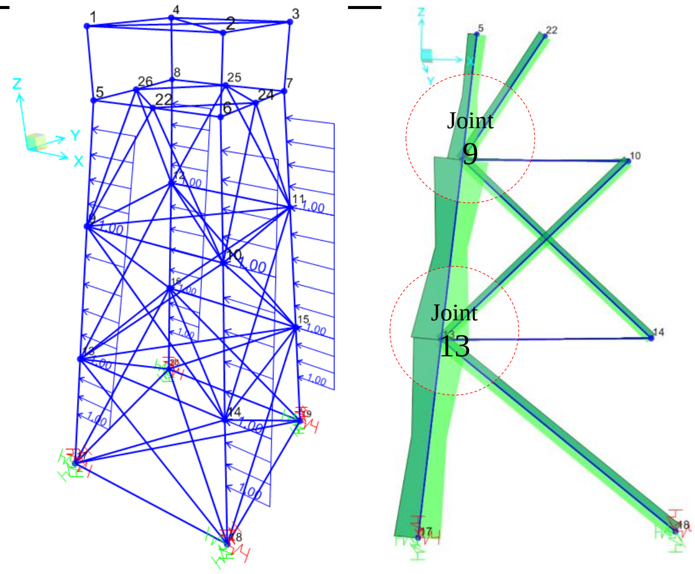

Fig. 6: Unidirectional wave loading and envelope stresses

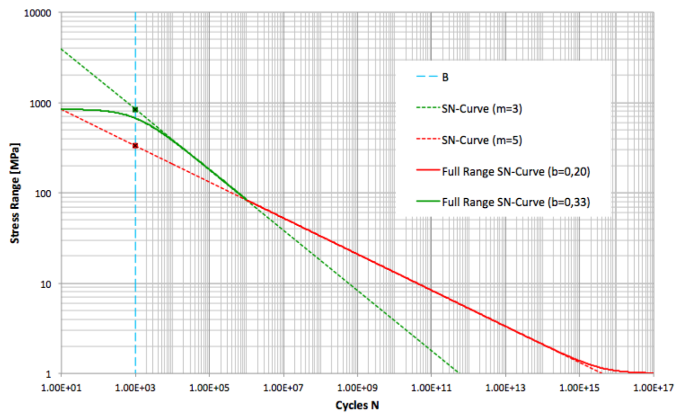

Fig. 7: Full range T-curve

strength amplitude or range for rotating bending test-based S-N curves, and it is the ultimate shear strength amplitude or range for torsional fatigue test-based S-N curves.

The same damage indicator D_i is further transformed to the next level, $i+1$, and the damage equivalent stress at the very same level is derived from the mathematical relation in Eq. 11.

$$D_i = \frac{\sigma_{(i)eq} - \sigma_i}{\sigma_u - \sigma_i} = \frac{\sigma'_{(i+1)eq} - \sigma_{i+1}}{\sigma_u - \sigma_{i+1}} \tag{11}$$

Further simplifications gives,

$$\sigma'_{(i+1)eq} = D_i(\sigma_u - \sigma_{i+1}) + \sigma_{i+1} \tag{12}$$

Where $s'_{(i+1)eq}$ is the damage equivalent stress at load level $i+1$ and the corresponding number of cycles to failure is denoted $N'_{(i+1)R}$. Applying an arbitrary stress amplitude or stress range $s_{(i+1)}$ at the level $i+1$ for $n_{(i+1)}$ number of cycles, gives a corresponding residual life as follows.

$$N_{(i+1)R} = N'_{(i+1)R} - n_{(i+1)} \tag{13}$$

The damage stress amplitude or stress range $s_{(i+1)eq}$ corresponding to $N_{(i+1)R}$ at load level $i+1$, is then obtained from the schematic presentation of the S-N curve in Fig. 3. Furthermore, the cumulative damage at the very same load level is defined as,

$$D_{(i+1)} = \frac{\sigma_{(i+1)eq} - \sigma_{i+1}}{\sigma_u - \sigma_{i+1}} \tag{14}$$

Similarly, the same procedure is followed until D_i is equal to one, where at this point $s_{(i)eq}$ becomes equal to the ultimate tensile strength stress amplitude s_u.

3 CASE STUDY

3.1 Considered jacket structure

A previous report on stochastic fatigue analysis of jacket type offshore structures is the case under consideration [11]. All structural elements of the jacket platform are modeled as tubular beam elements of steel grade S355. The main dimensions of the steel jacket are 27m x 27m x 62.5m in the global x-, y-, and z-direction respectively. The cross-sectional diameters and thicknesses are as defined in Table 1. The total mass of the deck is assumed to be 4.8·106 kg [11] and is distributed to the deck plane connections as point loads. The foundation plane is consisting of four connections modeled as flexible springs of the linear elastic nature. Spring properties are presented in Table 2. Further, the principles of the design of the steel jacket are in reference with NORSOK-N004.

3.2 Wave load modeling

The idea is to simulate different waves, obtain the corresponding hydrodynamic loads based on the theory presented in section 2.1.1 and assign these loads to the jacket model. The wave simulation is based on a scatter diagram for the Northern North Sea (1973-2001)[12], which gives a description of the sea state, the probability of the occurrence usually expressed in the number of observations during a period of time and the expected energy corresponding to each sea state [6]. Three different wave heights are chose in reference with the previously mentioned scatter diagram (Table 3), and are assumed to be consecutively generating during the course of one day. Further, for short-term wave conditions, the sea state is in general assumed to be stationary for an interval of 20 minutes up to 3- or 6- hours [5, 6]. Hence, each sea state is in this case assumed to be stationary for 3 hours and the wave loads acting on the platform legs are to be calculated from the maximum wave height . Having obtained the wave loads for each sea state, we are able to extrapolate and plot time-history functions. The time-history functions are extrapolated for a 24-hour period, making these functions valid for one single day. Further, it is assumed that the structure will have this loading history throughout its service life. Meaning that we now have a simplified simulation of three waves of different wave heights, being generated consecutively after one another during the course of one single day.

3.3 Finite element analysis

A comprehensive, state-of-the art FEM software for the design and analysis of civil structures (SAP2000) is used for static design- and dynamic time history analysis of the structure. In order to ensure adequate static design, several verification steps are taken, amongst them:

- A design-check of the structure is performed in order to ensure that no member exceeds the capacity given by the design code.
- Verification that all steel frames pass the stress-capacity ratio.

Table 3: Cross-sectional data of the frame elements [11]

Members	Diameter [m]	Thickness [mm]
Deck legs	2.0	50.0
Jacket legs	1.2	16.0
Braces in the vertical plane	1.2	16.0
Braces in the horizontal plane		
Elevation: +5m	0.8	8.0
-10m	1.2	14.0
-30m	1.2	14.0
-30m (diagonals)	1.2	16.0
-50m	1.2	14.0

Table 2: Spring properties [11]

Horizontal stiffness	$1.2 \cdot 10^5\ kN/m$
Vertical stiffness	$1.0 \cdot 10^6\ kN/m$
Rotational stiffness	$1.2 \cdot 10^6\ kNm/rad$

Table 3: Most observed wave heights and the corresponding peak periods [12]

H_s [m]	t_p [s]
1.5	9
2.0	9
2.5	9

Furthermore, a dynamic time-history analysis gives the stress distribution of each frame element, allowing us to identify the two most critical connections, which are singled in Fig. 13, and are used as reference points. Nominal stresses due to axial load, in-plane and out-of-plane bending moment for each frame element are plotted as time-history functions. These functions are used as reference points when determining the stress concentration factors for each structural component in each connection, and are reference points for hot spot stress history evaluation.

4 FATIGUE LIFE ESTIMATION

4.1 Fatigue life estimation based on the conventional approach

The failure criteria when estimating fatigue life on the basis of the conventional theory of a linear cumulative damage is commonly expressed by the Palmgren-Miner hypothesis and a fatigue design factor [8].

$$D = \sum_{i=1}^{k} \frac{n_i}{N_i} = \frac{1}{a} \sum_{i=1}^{k_b} n_i \,.\, (\Delta\sigma_i)^m \leq \frac{1}{FDF} \tag{15}$$

where

k_b – number of stress blocks

FDF – fatigue design factor

Classification of the fatigue design factor is depending on the significance of the structural component, with emphasis being put on structural integrity and availability for inspection (or repair). First and foremost, one has to determine whether failure of the structural component will lead to danger of loss of human lives, environmental pollution and whether there are financial consequences. Both NORSOK and Det Norske Veritas provide clear guidelines for assessing this matter.

Fatigue assessment is based on the deterministic method, which is often considered to be a simplification of the spectral method. The deterministic approach is applicable when there is a linear relation between wave loads and the structural response due to these loads. Results of the fatigue life estimation until crack initiation are presented in the following tables.

Results show that the chord governs the fatigue life of each joint. Hence, the chord in joint 9 will govern the fatigue life of the whole jacket platform – with a fatigue life of 17 years. Fatigue life estimations are based on fatigue design factors of 1 and 3. The “real” fatigue of the chord will be somewhere between these two parameters. Note that the there is a linear relation between the results obtained from the Palmgren-Miner theory.

Table 7: Fatigue life estimation of joint 9

			Fatigue Life in Years	
	Element No.	Member	FDF=1	FDF=3
Joint 9	32	Chord	50	17
	63	Brace A	∞	∞
	13	Brace B	∞	∞
	56	Brace C	∞	315

Where members labeled ∞, are not subjected to fatigue.

Table 7: Fatigue life estimation of joint 13

			Fatigue Life in Years	
	Element No.	Member	FDF=1	FDF=3
Joint 13	32	Chord	90	30
	55	Brace A	∞	∞
	19	Brace B	∞	∞
	48	Brace C	∞	369

Where members labeled ∞, are not subjected to fatigue.

Table 7: Fatigue life estimation of joint 9 based on the damage indicator sequential law

			Fatigue Life in Years	
	Element No.	Member	FDF=1	FDF=3
Joint 9	32	Chord	41	13
	63	Brace A	NA	NA
	13	Brace B	NA	NA
	56	Brace C	NA	261

Table 7: Fatigue life estimation of joint 13 based on the damage indicator sequential law

			Fatigue Life in Years	
	Element No.	Member	FDF=1	FDF=3
Joint 13	32	Chord	74	24
	55	Brace A	NA	NA
	19	Brace B	NA	NA
	48	Brace C	NA	306

4.2 Full range T-curve in sea water with cathodic protection

A technique for defining the full range S-N curve was presented in the previous section. However, fatigue assessment of tubular members is to be based on T-curves. The design code T-curve provided by DNV-RP-C203 consists of two different curve slopes. A point in question is how to obtain the full range T-curve. A proposed approach is presented in section 3. First and foremost, the design code given T-curve stretches along the green dotted line all the way to the intersection with the red curve. It then stretches along the red curve all the way to interception point $\sigma_{\infty}=1$. The intersection between the green and red curve represents change in the negative inverse slope of the T-curve. The red curve represents the full range of a T-curve with tangent slope $b=0.20$. The green curve represents the full range of a T-curve with tangent slope $b=0.33$. Both full range curves are obtained from Eq. 9.

A proposed approach is to base the fatigue assessment on the red colour curve. This is because stress history evaluation shows that the stress ranges for each structural component are in the high cycle region. Further, the stress ranges for each structural component are in the region where $m=5$ $(\Delta\sigma \lesssim 83 MPa)$. However, the fatigue testing data of the specimen and the application of the full range technique would allow us to plot a more accurate full range curve.

The full range T-curve presented in section 3 is the basis for the sequential law fatigue life estimations.

4.3 Fatigue life estimation based on the proposed approach

The proposed fatigue approach for uniaxial fatigue is utilized to predict the fatigue life until crack initiation. Fatigue life estimations in this section consist of huge numerical iterations of the sequential law (2.3), and the associated full range S-N curve. The structural components that were proven to not be subjected to fatigue in Section 4.1 are not attended in this section. Results obtained are presented in Table 6 and 7.

Results show that the chord in joint 9, with a fatigue life of 13 years (for a FDF of 3) remains the governing member both from a local, and global point of view. Further, it is observed that there no longer is a linear relation between the fatigue life where FDF is equal to 1 versus the fatigue life for the case consisting of a FDF of 3. Furthermore, it is observed that for the case where the FDF is three, results show a reduction of ca. 24% from the Miner`s prediction (consisting of the same FDF).

Results presented in Table 7 show that the chord remains the governing member with respect to local assessment of joint 13. However, joint 9 governs the fatigue life of the jacket structure. Outputs from the damage indicator based sequential law and the associated full range S-N curve show a reduction of each structural member somewhere in the range of 17-24% (depending from one member to another). Even though results show significant deviation, one should point out that verification results presented in Fig. 12 show cases where the deviations are of significantly higher amounts.

5 CONCLUSIONS

A new damage indicator based sequential law employed new approach was proposed to estimate fatigue life of offshore steel structures. A verification of the new damage model was conducted by comparing the theoretically predicted damage and fatigue life with experimentally observed damage and fatigue life respectively. The proposed approach was further utilized to estimate the fatigue life of offshore jacket structure as a case study.

The experimental verification reveals that the proposed approach, which consists of a new damage indicator and a fully known S-N curve modeling technique, provides a more realistic fatigue life than the Miner's employed conventional approach. Wave-structure interaction was reasonably modeled based on linear wave theory and the Morrison's load formula. Definition of the sea state was based on a scatter diagram valid for the North Sea. The FEM- employed time history analysis was conducted to obtain the nominal stress histories of all legs/chords and braces. Then hot spot stress histories of critical tubular joints were obtained by SCF's given in DNV-RP-C203. The DNV-RP-C203 code provided S-N curve of tubular joint was transferred to a full range S-N curve by applying the proposed technique. Then new damage indicator based sequential law was applied to estimate the fatigue lives of the identified critical joints. The fatigue lives were also estimated by employing Miner's conventional approach. Comparisons of these fatigue lives show a ≈17-24% deviation. This deviation of the remaining fatigue lives highlights the necessity of a proper fatigue theory, which describes the loading sequence effect precisely, and the importance of a reasonably accurate approach to predict the stress histories. Designing for 20 years of lifetime, a difference of ~25% in fatigue life estimation in either direction (~25% less or more), results in a significant amount of years.

Finally, these observations tend to conclude that the application of the damage indicator-based sequential law is advisable for the evaluation of fatigue lives of offshore jacket structures. Further verification and comparison between different case studies is recommended for future studies in order to ensure the precision of the proposed approach. Future case studies will be based on more realistic wave measurements and their corresponding sequence.

REFERENCES

[1] Almar-Næss, A., 1985, "Fatigue Handbook – Offshore Steel Structures"

[2] Vårdal, O.T., Moan, T., Hellevig, N.C., 1999, "Comparison Between Observed and Predicted Characteristics of Fatigue Cracks in North Sea Jackets", Offshore Technology Conference

[3] Moan, T., 2007, "Fatigue Reliability of Marine Structures, from the Alexander Kielland Accident to Life Cycle Assessment", International Journal of Offshore and Polar Engineering, Vol. 17, pp.1-21

[4] Mesmacque, G., Garcia, S., Amrouche, A., Rubio-Gonzalez. C., 2005, "Sequential Law in Multiaxial Fatigue, A New Damage Indicator", International Journal of Fatigue, 27(4), pp-461-467

[5] Det Norske Veritas, 2010, "DNV-RP-C205: Environmental Conditions and Environmental Loads"

[6] Larsen, C.M., 2005, "Notes for Special Topics: Dynamic Analysis of Marine Structures", Norwegian University of Science and Technology

[7] Gudmestad, O.T., Connor, J.J., 1983, "Linearization Methods and the Influence of Current on the Nonlinear Hydrodynamic Force", Applied Ocean Research Vol. 5 No. 4

[8] Det Norske Veritas, 2012, "DNV-RP-C203: Fatigue design of Offshore Steen Structures"

[9] Siriwardane, S.A.S.C., Ohga, M., Dissanayake, P.B.R., Kaita, T., 2010, "Structural Appraisal-Based Different Approach to Estimate the Remaining Fatigue Life of Railway Bridges", Structural Health Monitoring: an International Journal, 9(4), 323-339.

[10] Siriwardane, S.A.S.C., Ohga, M., Dissanayake, P.B.R., Kaita, T., 2009, "Remaining Fatigue Life Estimation of Existing Railway Bridges", Railway Transportation: Policies, Technology and Perspectives, (Book Chapter), pp.181-216.

[11] Sigurdsson, G., 1988, "Stochastic Fatigue Analysis of Jacket Type Offshore Structures", ISSN 0902-7513 R8828

[12] Chakrabarti, S.K., 2005, "Handbook of Offshore Engineering Volume 1".