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Methodology for modeling and service life monitoring of mooring lines of floating wind turbines

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ABSTRACT

Nowadays, the service life monitoring of mooring lines attracts more and more attention from the marine renewable energy (MRE) industry where developers desire to have a closer look on the safety margin. Besides, current standards are also being improved in order to ensure safe and cost-effective design for MRE devices. In the present study, we believe that monitoring and modeling should be implemented in parallel in order to better estimate the actual state and the reliability of the system during the expected service life. Besides, it is crucial to clarify the mechanic of the mooring line responses, to understand why and how they are modified during deployment, and how those can be accounted for in order to update the actual fatigue damage and predict the remaining allowable service life of the system. A comprehensive modeling and service life monitoring methodology for mooring lines of floating wind turbines (FWTs) is discussed by the authors based on the reliability approach. Critical factors that influence structural behaviors such as material deterioration and marine environmental factors involved are identified and accounted for in this reliability-monitoring basis. Ultimately, based on the reliability level estimated, decisions regarding required maintenance or replacement of lines can be made.

Key words: Floating Wind Turbine; Service life monitoring; Reliability; Mooring line; FLS; ULS.

1 Introduction

The modeling and monitoring of offshore mooring lines require a sound understanding of both the relating mechanical behaviors of all the mooring components (i.e. load-strain behaviors, breaking strength, critical modes of failure, etc.) and the marine environmental factors (i.e. environmental loading, corrosion, abrasion, colonization of marine species, etc.) that influence such behaviors. The design of offshore mooring lines are often checked against three limit states (DNV OS-E301, 2011):

- Ultimate limit state (ULS) where the maximum tension in the lines is designed to remain below the minimum breaking strength of the mooring components. A good estimate of the working tension is, therefore, mandatory for a reliable design. It also means that in order to perform a detailed mooring design, the mechanical properties (i.e. load-strain properties, breaking strength, etc.) of all the mooring components and the relating deterioration factors must be well understood.
- Fatigue limit state (FLS) where mooring lines are designed for a certain service life (usually between 20 and 25 years for FWTs). The fatigue life of a mooring line is affected by not only the mechanical fatigue behaviors of the materials composing that line but also the material deterioration due to the marine environmental factors.
- Accident limit state (ALS) where the mooring lines are designed with a certain redundancy in order to ensure their capability of surviving against the failure of one mooring line.

The previous issues have been subjects of a large amount of research work (Aqdam et al., 2018; Xue et al., 2018; Wu et al., 2015). Ma et al. (2013) investigated several accidental events in permanent mooring systems and found that many line failures occurred in early service years of mooring lines. Even if fatigue is generally the dominant cause, Gryphon incident (Finucane, 2012) teaches us a case of failure due to a combination of failure of heading control and overload. Moreover, the failure mechanisms have surprised the industry experts. A limit number of known failures for mooring lines of marine renewable energy devices (e.g. Oceanlinx in 2010 and Wavedragon in 2004) has been discussed by Christensen et al. (2005), but the reasons of failure stay still unclear. Brindley et al. (2014) stressed that only strength requirements do not fully reflect the system reliability. Pham et al. (2004), Pham (2018) studied the interaction between the reliability in the ULS and FLS. According to their conclusions, the total reliability should be considered either by multiplying the reliability of the two design criteria (for the reliability in the ULS case) or by adding the fatigue damage caused by extreme sea states that might probably occur at any point during the expected service life of structures (for the reliability in the FLS case).

Unlike O&G platforms, MRE devices are generally small compared to the incident wave length and thus complex motions may occur which may be large if the system response is resonant (Weller et al., 2013). Moreover, strong coupling between the device and mooring system is usually observed. For the FLOATGEN project that the present case study is based on, nylon is used as a principal mooring component and the modeling of mooring responses lacks validation. It is mainly due to the non-linear load-elongation behaviors and fatigue characteristics of nylon mooring ropes that can not be captured following the same testing and

modeling procedures for polyester as recommended by the current Oil & Gas (O&G) standards.

Thies et al. (2014) proposed, other than numerical modeling, an alternative approach to predict fatigue damage using directly in-situ load measurements. Their approach has been concluded to allow a better fatigue assessment and an optimization of safety factors at the design state where on-site measurements are available. Also, the approach can be used for the in situ monitoring in order to predict the fatigue damage and the remaining service life of the structure during deployment. However, the authors believe that only load measurements are not enough to characterize the actual state of mooring lines that actually includes the likely modified mooring line mechanical behaviors (e.g. strength and stiffness reduction, etc. due to material loss Gao et al., 2005; Liu et al., 2015), modified mass and hydrodynamic damping due to biofouling, and other marine environmental factors (e.g., corrosion, wear, etc.). These mechanical behaviors of mooring materials and marine environmental factors should be fully understood and accounted for. Indeed, it is vital to clarify the mechanic of the mooring line responses, to understand why and how they might be modified during deployment, and how those can be accounted for in order to update the actual fatigue damage and predict the remaining allowable service life of the system. That is why, in our opinion, modeling and monitoring should be implemented in parallel in order to better control and update the actual state of mooring lines, better estimate the fatigue damage and reliability of the system during deployment. Based on that, decisions regarding required maintenance or replacement of lines can be made.

The present modeling and service life monitoring methodology for mooring lines of FWTs is based on the reliability approach. The methodology is applied in the following for the ultimate limit state (ULS) in order to check if the mooring lines can still sustain the design extreme load case after years of operation. Also, by applying the methodology for the fatigue limit state (FLS), the remaining allowable service life can be predicted based on the information obtained from the previous operating years. Furthermore, the present methodology can also be considered for a more detailed mooring design once the information from similar structures, projects, sites, etc. are known.

The defined case study is inspired from the hybrid mooring lines of the 2 MW FWT installed at the SEM-REV test site since 2017 as part of the FP7-EU FLOATGEN project (Pham et al., 2019; Berhault, 2017; Spraul et al., 2017; Berhault et al., 2016). We focus on the mechanical behaviors of two mooring components (i.e. chain and nylon), the material deterioration during operation and the marine environmental factors that influence such behaviors. Once the previously mentioned information is fully understood and accounted for, the reliability following the service life of mooring lines can be better predicted. The corresponding level of reliability is then used to decide whether maintenance or replacement of lines is necessary.

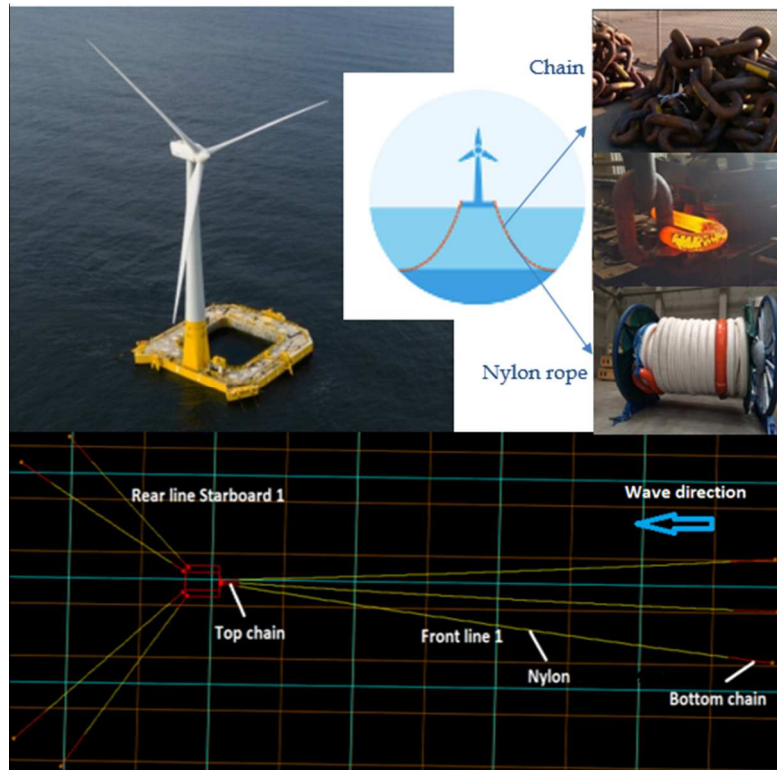


Fig. 1. The FLOATGEN FWT principal parameters and mooring components (Berhault, 2017) – up; and the Orcaflex model of the floating structure attached to its mooring lines (Spraul et al., 2017; Pham et al., 2019) – down.

The FLOATGEN FWT's principal mooring components and the Orcaflex model of its hybrid mooring lines connected to the floater are illustrated in Fig. 1 in which a line composed of top and bottom chains with a nylon section in between. A similar floating structure and mooring configuration have been published by Spraul et al. (2017), Pham et al. (2019) based on the publically available information from IDEOL and the Ecole Centrale de Nantes (Berhault, 2017). The hydrodynamic database is calculated by HydroStar (Bureau Veritas, 2016). However, ones should be aware that the floater and mooring characteristics used in this study are selected only for the study purpose and are not representative of the IDEOL's real floating structure and the FLOATGEN's mooring system.

2 Mechanical behaviors of mooring components for the modeling and monitoring of mooring lines

In order to apply the present methodology on the mooring lines of the FWT mentioned above, this section will briefly discuss the mechanical behaviors of two mooring components, i.e. chain and nylon and the marine environmental factors that influence such behaviors.

2.1 Chain

With more than 50000 tons in service according to the data from J. Fernandez et al. (2014), a huge effort has been putting on studying the characteristics and behaviors of chain mooring lines. The outcomes of several JIPs have been integrated to the offshore standards such as API

RP 2SK (2005), DNV OS E301 (2011), Bureau Veritas Guidance Note NI 493 (2015), etc. Together with wire rope and connector (e.g. shackles, H-link, etc.), chain make one of the top three components that caused most incidents in mooring lines. Some failure mechanisms are listed by Ma et al. (2013) such as out-of-plane bending (OPB), chain hocking, flawed flash welds, pitting corrosion, etc. Therefore, it is vital to understand all these mechanisms in order to identify the potential weak points of the lines, why and how the structural responses are modified during operation, and finally how to consider them in strength and fatigue design.

In the present study, in order to illustrate how the service life monitoring methodology can be applied, we focus on the corrosion of chains due to the marine environment as this phenomenon can lead to significant loss of material and likely modified mooring responses. Indeed, experiences from the Oil & Gas industry show several mooring line failures due to the fatigue damage induced by the crack propagation which can be accelerated by corrosion in marine environment and wear between chain links or at connection points (Lardier et al., 2008). Corrosion results in the reduction of steel surface, variation in residual stresses, therefore, influences both strength and fatigue design of mooring lines. Corrosion in sea water of chains is a complex phenomenon since it depends on both the mooring material properties and the marine environment characteristics (the composition of sea water, temperature, salinity, dissolved oxygen, etc.). Most of corrosion models are simply based on a constant rate of corrosion. For instances, API RP 2SK (2005) recommends an industry practice to increase the chain diameter in the splash zone and the dip or thrash zone on hard bottom by 0.2 to 0.4 mm/year, this increasing diameter is reduced to 0.1 to 0.2 mm/year for the remaining length. Bureau Veritas Guidance Note NI 493 (2015) requires a 0.4 mm/years of corrosion and wear allowance for the chain section in splash zone and the bottom length, and 0.3mm/year in the remaining length. DNV OS E301 (2011) recommends considering a corrosion rate of 0.4 mm/year in chain diameter in the catenary and bottom parts and a higher rate of 0.8 mm/year for the splash zone. Melchers et al. (2007) proposed a probabilistic model for fully submerged chains with the mean value of corrosion rate about 0.44mm/year with a standard deviation of 0.1mm/year. Schoefs et al. (2009) suggested an inspection model of the steel loss in view to account for the variability of the corrosion rate with time.

2.2 Nylon

For pilot farms of floating wind turbines (FWTs) in shallow-water between 50m and 100m, the application of nylon as a mooring component can provide a more cost-effective design. Indeed, nylon is a preferred candidate over polyester for FWT mooring because of its lower stiffness and a corresponding capacity of reducing maximum tensions in the mooring system. The use of nylon has, however, not been well proven appropriate for permanent mooring application due to its poor wet fatigue characteristics.

The main mechanisms of fatigue for synthetic ropes are creep, hysteresis heating, internal abrasion, axial compression, etc. (Hearle et al., 1993; Parsey, 1982). It is believed that hysteresis heating is not a concern for offshore mooring applications where ropes remain under-water throughout its service life. Likewise, the axial compression fatigue affects only stiff fibers such as aramid, thus, should not be a concern for nylon. However, the design practice for polyester mooring lines recommended by Bureau Veritas NI 432 (2007) is to keep the minimum tension not lower than 2% of minimum breaking strength (MBS) to avoid axial compression fatigue. The internal abrasion and creep, therefore, remain two critical fatigue modes for nylon ropes.

Creep is a static fatigue damage mode that ropes fail after a certain duration of time at a constant tension. Creep is either recoverable (primary creep - at low tension) or non-recoverable (secondary creep – at high tension). The latter is relevant to creep fatigue failure of fiber ropes. Creep fatigue failure can be critical for typical nylon that might fail in less than 5 hours being tensioned at 80% of its breaking strength (McKenna et al., 2004). For wet nylon 66, the creep process is the dominating fatigue mechanism for load levels above 30% its breaking strength. For cyclic loading, the creep effect is the same as if the peak load (i.e. maximum load) has been applied continuously. The creep fatigue mechanism is independent of the frequency of loading and can be predicted by the linear relationship between the maximum load and the log time to failure in second. Creep effect doesn't lead to the reduction of strength of ropes at least up to 75 to 80% of the fatigue life time (Mandell, 1987; Mandell et al., 1987).

Internal abrasion is a dynamic fatigue damage mode that the rope fails after a certain number of cycles under cyclic loading. Internal abrasion is caused by relative motions between strands which are directly related to the tension range and inversely related to the mean tension. Therefore, it can be concluded that the severity of internal abrasion is directly related to the tension amplitude and inversely related to the mean tension (Flory et al., 2006). Internal abrasion is a serious problem for highly structured, wet nylon ropes at moderate tension (McKenna et al., 2004). Severe internal abrasion can lead to significant loss of strength of ropes and, therefore, might modify mooring system behaviors and even lead to failure in ultimate and fatigue conditions. The internal abrasion damage can be reduced by recent fiber rope technologies such as the application of long-lay twisted ropes and special marine finish (Banfield et al., 2017).

3 Marine environmental impacts

The environmental factors that influence the modeling and health monitoring of mooring lines include the water depth, the environmental loads (e.g. wind, current, wave), marine species, water temperature, salinity, dissolved oxygen, etc.

Spraul et al. (2017), Yang et al. (2017) and Wright et al. (2016) studied the effects of marine growth on the mooring lines of marine renewable energy devices. According to their conclusions, the biofouling has an important impact on the tension responses and the fatigue life of mooring lines. For fiber rope mooring without covering and filters, junior harsh marine species such as mussels can even penetrate and growth inside the ropes, then accelerate the inter-strand abrasion (Weller et al., 2013). The marine species colonization measured at the Ecole Centrale Nantes/SEM-REV test site (Spraul et al., 2017) is shown in Fig. 2 for illustration.

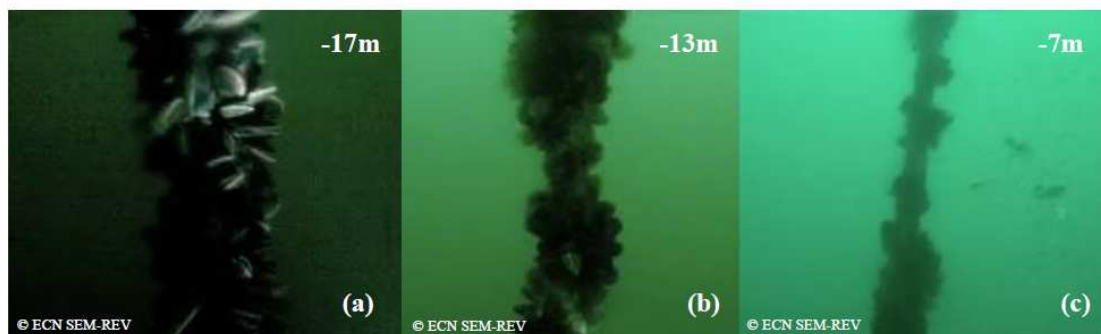


Fig. 2. Biofouling colonization survey by under-water camera at the SEM-REV test site (Spraul et al., 2017).

Other marine environmental effects such as accelerated corrosion of chain links in sea water, wear at the fairlead connection point and seabed touchdown sections of the lines, etc. should also be listed and understood for a comprehensive modeling and service life monitoring approach. The corrosion observed in the catenary mooring lines of a meteocean buoy operated at the Ecole Centrale Nantes/SEM-REV sea test site (Berhault, 2017) is illustrated in Fig. 3.



Fig. 3. The corroded chain mooring lines of a meteocean buoy at the SEM-REV sea test site (Berhault, 2017).

4 Frequency-domain method for fatigue damage estimate

The frequency-domain fatigue analysis approaches (Gao et al., 2007; Benasciutti et al., 2004, Tovo et al., 2002; Han et al., 2016; Ma et al., 2018) have been studied in order to accurately predict the fatigue damage without having to perform time-consuming Rain Flow Counting (RFC) technique in time-domain approach. Moreover, the analytical formula used to calculate the damage based on the chosen frequency-domain method can be applied directly to reliability analysis. For the service life monitoring of mooring lines, the procedure is interesting as it can be applied directly on the measured tension responses. In this study, as an example, the time domain dynamic mooring analysis is performed using OrcaFlex in order to get the time histories of the tension responses that will be latter considered as measured signals. The considered measured tension time histories are then transformed to power spectral density (PSD) in frequency domain by using fast Fourier transformation (FFT) in Matlab as illustrated in Fig. 4. Since the so-called measured tension time histories used in the present work are obtained from numerical simulations as mention above, they can be considered as non-noisy signals. However, it should be noted that measurement noise is a critical issue to be considered, which may add extra power to the PSD curve.

API-RP-2SK (2005) and DNV-OS-301 (2011) recommend T-N and S-N curves to calculate the fatigue damage of different mooring materials. In this study, the fatigue damage is calculated following the S-N curve approach since the uncertainties of the DNV's S-N curve can be known from the publications of Lardier et al. (2008) and Mathisen et al. (2005).

The fatigue curve is used for calculating the fatigue lives of mooring components as

$$N = K.S^{-m} \quad (1)$$

where N is the number of cycle to failure under a stress range S

K, m are the S-N curves parameters

The fatigue damage accumulation is calculated following the Palmgren-Miner's rule as

$$D = \sum_{i=1}^n \frac{1}{N(S_i)} = \frac{1}{K} \sum_{i=1}^n S_i^m \quad (2)$$

where S_i is the stress range of the stress cycle number i .

n is the number of stress cycles S_i .

If the stress process, $X(t)$, is a narrow-band Gaussian process, its amplitude follows a Rayleigh distribution. The expected damage due to $X(t)$ in a duration of time T (in seconds) is calculated as

$$\bar{D}_{NB} = \frac{\nu_0 T}{K} (2\sqrt{2}\sigma_X)^m \Gamma\left(\frac{m}{2} + 1\right) \quad (3)$$

where Γ - Gamma function : $\Gamma(z) = \int_0^{\infty} \alpha^{z-1} e^{-\alpha} d\alpha$

σ_X - Standard deviation of the response

ν_0 - mean zero-upcrossing rate $\nu_0 = \sqrt{\frac{\lambda_2}{\lambda_0}}$

λ_n is the n-th spectral moments: $\lambda_n = \int_0^{\infty} f^n W_X(f) df$

$W_X(f)$ is the Power Spectral Density of the response.

When $X(t)$ is a non-linear and wide-band process, the assumption of its Rayleigh-distributed amplitude is not valid. For this, the frequency-domain fatigue analysis approaches (Gao et al., 2007; Benasciutti et al., 2004; Tovo et al., 2002; Han et al., 2016; Ma et al., 2018) have been studied in order to select the most suitable approach to calculate the fatigue damage. The idea is, first, to calculate the narrow-band fatigue damage according to Eq. (3), then correct it with adjusted coefficients to account for the actual wide-band and non-linear characteristics of the tension (stress) responses. The fatigue damage calculated by these approaches are compared with the time-domain fatigue analysis for 20 different sea states. As shown in the Fig. 5, the Tovo & Benasciutti approach with the non-linear coefficient suggested by Winterstein (1988), Braccési et al. (2009), Cianetti et al. (2017) provides very good fatigue estimate comparing with the time-domain approach. The so-called D_TB+C approach is chosen to calculate the

fatigue damage of mooring lines and to perform reliability analysis for the following sections of the paper. Based on that, the fatigue damage is calculated as

$$\bar{D}_{TB+C} = \lambda_{NG} \cdot \rho_{WB} \cdot \bar{D}_{NB} \quad (4)$$

where ρ_{WB} is wide-band corrected coefficient: $\rho_{WB} = [b + (1-b) \cdot \alpha_2^{m-1}]$;

$$b \cong \frac{(\alpha_1 - \alpha_2) [1.112(1 + \alpha_1 \alpha_2 - (\alpha_1 + \alpha_2)) e^{2.11 \alpha_2} + (\alpha_1 - \alpha_2)]}{(\alpha_2 - 1)^2} ;$$

$$\alpha_1, \alpha_2 \text{ are the band-width parameters: } \alpha_1 = \frac{\lambda_1}{\sqrt{\lambda_0 \lambda_2}} ; \alpha_2 = \frac{\lambda_2}{\sqrt{\lambda_0 \lambda_4}} ;$$

λ_{NG} is the non-Gaussian corrected coefficient;

$$\lambda_{NG} = \frac{D_{NG}}{D_G} = \exp\left(\frac{m^{3/2}}{\pi} \left(\frac{k_u - 3}{5} - \frac{s_k^2}{4}\right)\right) \text{ for } k_u < 5; \text{ (Braccesi et al., 2009) or}$$

$$\lambda_{NG} = \frac{D_{NG}}{D_G} = \exp\left(\frac{m^{3/2}}{(0.156 + 0.416 k_u) \pi} \left(\frac{k_u - 3}{5}\right)\right) \text{ for } k_u > 5 \text{ (Cianetti et al., 2017)}$$

s_k and k_u are the skewness and kurtosis of the process $X(t)$.

This expected fatigue damage is rewritten as

$$\bar{D}_{TB+C} = \lambda_{NG} \cdot [b + (1-b) \cdot \alpha_2^{m-1}] \cdot \bar{D}_{NB} \quad (5)$$

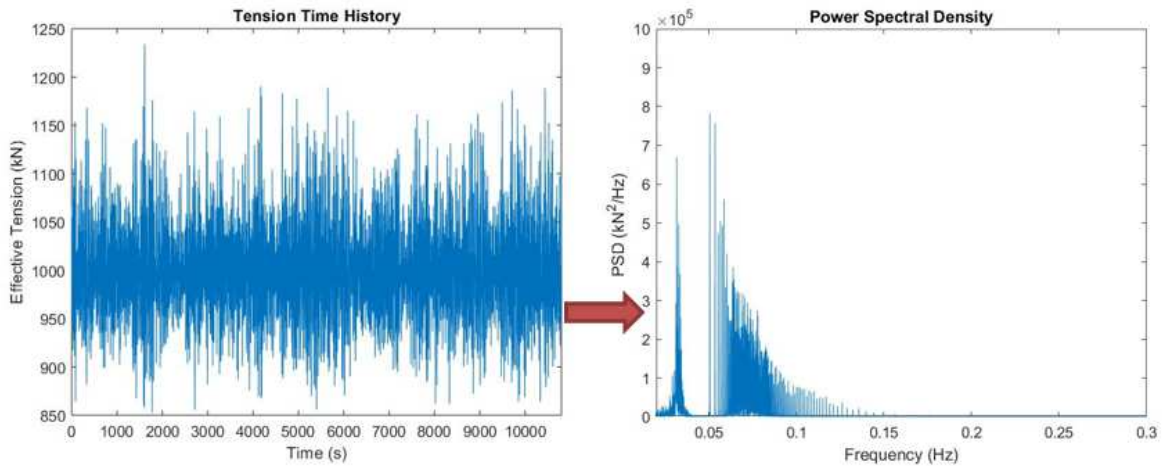


Fig. 4. The tension response PSD obtained from the tension time history calculated by Orcaflex using fast Fourier transformation (FFT).

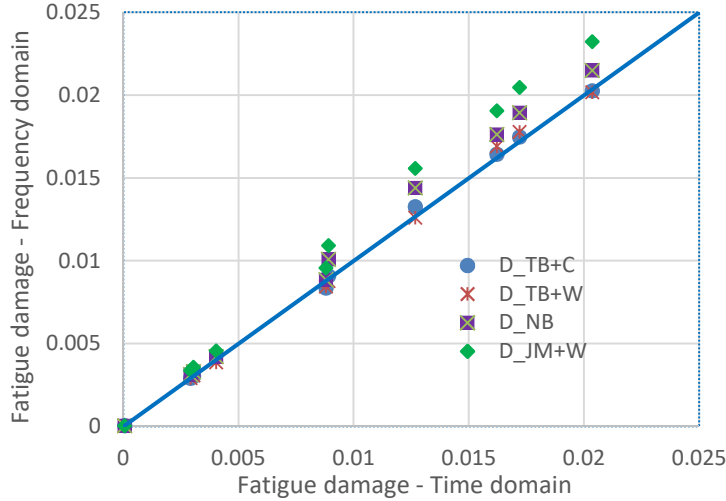


Fig. 5. Comparison of fatigue damage calculated by frequency-domain and time-domain methods. D_TB+C: the Tovo & Benasciutti approach (Benasciutti et al., 2004; Tovo et al., 2002) with the non-linear coefficient proposed by Cianetti et al. (2009) or Braccesi et al. (2017); D_TB+W: the Tovo & Benasciutti approach (Benasciutti et al., 2004; Tovo et al., 2002) with the non-linear coefficient proposed by Winterstein (1988); D_NB: The narrow-band approach; D_JM+W: the Jiao & Moan approach (Gao et al., 2007) with the non-linear coefficient proposed by Winterstein (1988).

5 Methodology for modeling and service life monitoring of mooring lines

A methodology for modeling and service life monitoring of mooring lines is discussed in the following based on the reliability approach. We believe that once the mechanical behaviors of mooring components and the influencing factors are fully understood, this modeling and service life monitoring approach can provide not only a better estimate of the cumulative fatigue damage but also a better prediction of the allowable remaining service life. On the one hand, the reliability approach allows accounting for the uncertainties of the materials and environmental loading (Pham et al., 2004; Pham, 2018; Rendón-Conde et al., 2015; Bitner-Gregersen et al., 1995; Horn et al., 2019). On the other hand, based on the corresponding required reliability level, the design safety coefficients can be optimized for a more cost-effective but reliable design. Finally, decisions regarding the required maintenance or replacement of mooring lines can be made based on the level of reliability predicted during operation.

The methodology is presented in Fig. 6 and Fig. 7 in which, first, the mechanical behaviors of all mooring components (i.e. strength, stiffness, fatigue characteristics, etc. as presented in block 2 of the flowchart) and the environmental factors that influence such behaviors (i.e. water depth, environmental loading, marine species, corrosion and wear, etc. as presented in block 1 of the flowchart) are identified. The measured environmental factors (block 1) and mechanical properties of mooring components (block 2) are input to mooring analysis tools to calculate the mooring response which is then checked against the measured one. Basically, for

the present methodology, it is very important to determine and clarify which changes will occur (i.e. material deterioration, modified mass and damping due to bio-colonization, modified length or pretension due to creep of the synthetic mooring components, etc.), and how to update those changes in the modeling and fatigue life monitoring of mooring lines. For instance, after years of deploying, the material loss (e.g. due to corrosion for chain or internal abrasion for nylon) may induce significant modifications on not only stiffness but also strength and fatigue characteristics of mooring components. Finally, the collecting information is updated in mooring analysis tools in order to better control the actual state of mooring lines. Finally, it is suggested that the mooring safety is assessed following the expected service life directly via reliability analysis in order to support making decisions regarding maintenance or replacement of lines (Fig. 7).

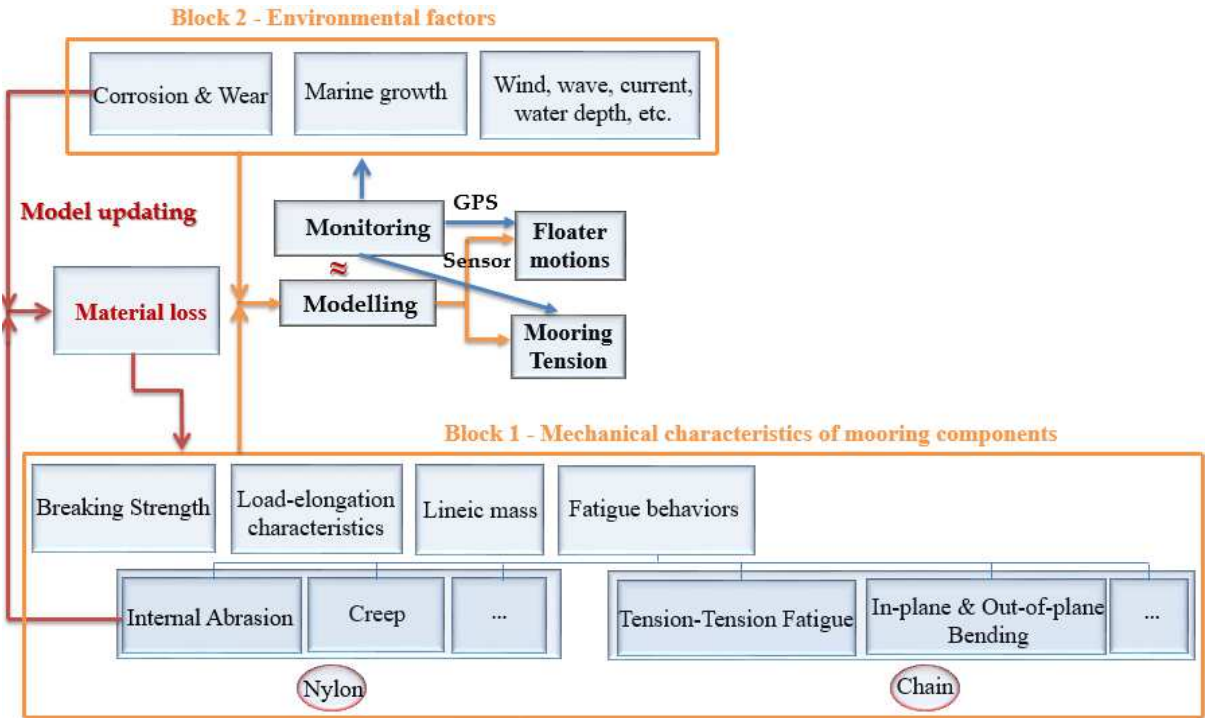


Fig. 6. Methodology for modeling and service life monitoring of mooring lines – model updating

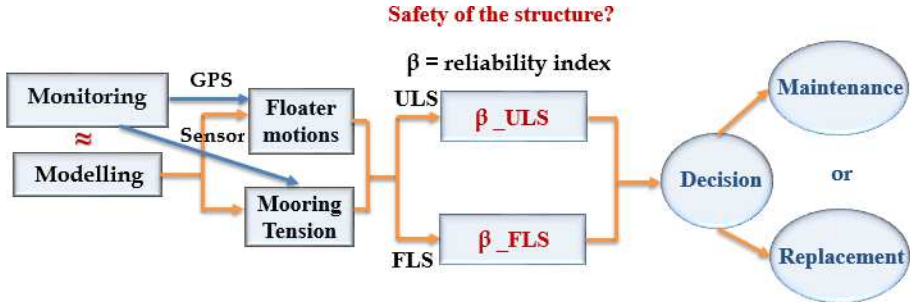


Fig. 7. Methodology for modeling and service life monitoring of mooring lines – decision making

As an example, we consider here the corrosion in mooring lines as an annually constant reduction of diameter of chain links. This phenomenon leads to material loss and likely modified mooring responses. The same exercises have been done by Gao et al. (2005) and Lardier et al. (2008) for the reliability of chain links accounting for the reduced breaking strength and the increased stress due to corrosion. In the present modeling and life monitoring methodology, we convince that the Thies et al.'s approach (Thies et al., 2014) to estimate mooring responses due to environmental loading based directly on field measurements should be applied. In addition, the likely modified structural behaviors should also be well considered in the modeling of line responses. In our opinion, once the modeling of mooring responses can be well implemented based on a full understanding of the mechanical factors involved, the modeling responses can be used to compare with the measured ones in order to re-evaluate the actual state of mooring lines. As a consequence, a full control of the actual state of mooring lines and also a better fatigue estimate can be obtained.

In the present exercise, the frequency domain fatigue analysis is chosen to calculate the fatigue damage directly from the measured tension responses. The advantages are two fold: first, the fatigue damage can be estimated without having to perform RFC technique as in time domain analysis, second, the analytical formula of the fatigue damage can be used directly for reliability analysis considering the uncertainties of the environmental loading and the SN fatigue curve of mooring components. The probabilities of failure (P_f) in the ULS and FLS are calculated following the service life of the mooring lines considering modified mechanical behaviors of mooring components and relating environmental factors. The P_f for ULS (P_{f_ULS}) is calculated to ensure a sufficient level of reliability of the system to sustain the design extreme sea states that might probably happen at any moment during the service life. The P_f for FLS (P_{f_FLS}) is estimated to ensure a sufficient level of reliability that the cumulative fatigue damage is inferior than the critical damage. Moreover, we also predict the reliability level in FLS considering the added damage due to the design extreme sea states (e.g. with a return period of 100, 50, 20, 10 years, etc.) assuming those might probably happen at any moment during the expected service life of lines. These probabilities of failures in ULS and FLS are used for making decisions regarding maintenance or replacement of lines.

It's worth to acknowledge that standards DNV OS-E301 (2011), API-RP-2SK (2005), Bureau Veritas NR 493 (2015) recommend to consider an additional thickness of chain links at the initial design stage in order to compensate the material loss due to corrosion. In our opinion, this is a good practice for Oil & Gas platforms mooring lines in deep-water where maintenance or replacement of lines are difficult and costly. However, that also increases the cost and the tension responses in the mooring system as they become more heavy and stiff. For mooring systems of MRE devices where it seems easier and less costly to maintain or replace damage lines, the present monitoring methodology might be a promising alternative to ensure a cost-effective design and safe operation. In the following example, instead of choosing a greater diameter of chain links to account for the corrosion effect, the mooring system is designed for the expected fatigue life of 20 years with a safety factor of 3 (Lardier et al., 2008). The probabilities of failure in ULS and FLS are calculated following the expected service life in order to investigate how material loss (e.g. due to corrosion as in the present example) influences the modeling and service life monitoring of mooring lines and how decisions on maintenance or replacement of lines can be made based on the estimated levels of reliability. Although the material loss due to the internal abrasion fatigue damage of the nylon rope is not considered here due to the lack of publically available data for this fatigue mechanism, it can be accounted for in the same manner by following the present methodology. The effect of marine growth on the hydrodynamic diameter, hydrodynamic

coefficient (Ca, Cd) and mass of the mooring lines is also a source of mooring response modification. However, for the sake of simplification, it is not considered here, which is the subject of a separate work (Spraul et al., 2017). The additional damage due to in-plane and out-of-plane bending (IPB and OPB) is also not considered in this example either. However, it should be stressed that in shallow water and at high pretension (i.e. greater than 10% of the minimum breaking strength of the top chain) IPB and OPB are important and the combined fatigue (i.e. tension-tension plus IPB and OPB) needs to be investigated (Bureau Veritas, 2014). This can be partly mitigated with an appropriate selection of the fairlead type. Also for simplification, in the present model, the wind turbine is considered only by modifying the weight and the COG of the floating platform model. The mean components of the wind and current loads are considered as lateral loads that result in additional amounts of the mean tension in the mooring lines. However, the coupled aero-hydrodynamic effect of the floating wind turbine should be carefully considered, which can be done by linking Orcaflex to FAST. Even though the subject of the present work is not on the aero-hydro coupling, we believe that the wind turbine modeling in the long-term can be implemented by the same manner as suggested by the present methodology (i.e. by identifying structural deterioration/changes during deployment and updating those changes in the model).

The time-dependent diameter of chain links considering the corrosion effect is determined as (Lardier et al., 2008)

$$d(t) = d_0 - t.R_{\text{corr}} \quad (6)$$

where t - the operation time in years; d_0 - the diameter of chain links at $t=0$,
 R_{corr} - the annual corrosion rate;

The time-dependent axial stiffness of the chain sections considering the corrosion effect is determined as (Orcina, 2016)

$$\begin{aligned} EA(t) &= 0.854 \times 10^8 d^2(t) \text{ (kN) for studless chain} \\ EA(t) &= 1.01 \times 10^8 d^2(t) \text{ (kN) for studlink chain} \end{aligned} \quad (7)$$

The corrosion in the chain sections are considered as a reduction in the diameter of chain links and the axial stiffness of the sections as in Eq. (6) and Eq. (7). Those are updated in the Orcaflex model to calculate the modified mooring responses.

The stress process $X(t)$ is calculated from the tension process $T_s(t)$ as

$$X(t) = \frac{T_s(t)}{A(t)} \quad (8)$$

where

$A(t)$ – the time-dependent cross section of the chain links, $A(t) = 2 \cdot \pi \frac{d^2(t)}{4}$

The time-dependent Minimum Breaking Strength of the chain links is determined as

$$\text{MBS}(t) = c^2 \cdot d^2(t) \cdot (44 - 0.08 \cdot d(t)) \quad (9)$$

where c is the grade dependent coefficient (Ramnäs, 2013).

6 Reliability assessment for the case study

It is commonly known that reliability plays a key role in the cost-effectiveness of the design where uncertainties are important and it is easy to implement in risk analysis. In the present case study, on the one hand, this allows assessing the probability that the structures can sustain the design extreme sea states along the design life time. On the other hand, the reliability level corresponding to cumulative fatigue damage during operation and the remaining service life of structures can be estimated to support decisions regarding required maintenance or the replacement of lines. It should also be noted that the severity in fatigue is dependent of the deploying site and should be investigated case by case. In the following example, due to the small discrepancy for comparativeness of the probability of failure, another estimate of reliability, i.e. the reliability index, is compared. The reliability analysis is performed based on the first order reliability method (FORM) using the open source initiative OpenTURNS (2016).

6.1 Reliability in strength condition (ULS)

It is commonly accepted by the offshore industry that for the duration of 3 to 6 hours (i.e. corresponding to the duration of an extreme sea state), the tension response process, $T_s(t)$, can be considered as stationary. Assuming that the process is normally distributed, the probability density function of the annual maximum tension ($T_{s\max}$) is expressed as

$$p(T_{s\max}) = \bar{T} + \frac{1}{\sigma_{T_s} \sqrt{2\pi}} \exp\left(-\frac{T_{s\max}^2}{2\sigma_{T_s}^2}\right) \quad (10)$$

where

\bar{T} - the mean tension due to the pre-tension and the mean environmental loads
 σ_{T_s} - the standard deviation of the tension response process $T_s(t)$.

It is highlighted that the assumption of Gaussian distribution may lead to an under-estimate of the extreme maximum tension response and a corresponding unrealistically low probability of failure, and in that case, a Gumbel distribution may be used instead. However, this Gaussian

assumption seems acceptable here as the purpose of the chosen case study is to illustrate the present methodology by showing the importance of updating the material loss during the expected service life in order to better estimate the reliability of the system.

The annual breaking tension of the chain links, $T_{BS}(t)$, is assumed to be log-normally distributed with the mean value equals to $1.2 \times MBS(t)$ and the coefficient of variation (COV) equals to 0.05 (Gao et al., 2005). These are presented in Tables 1 and 2 where the material loss in the chain links due to corrosion is considered as an annually reduced of diameter, R_{corr} equals to 0.4 mm/year and 0.8 mm/year. The time history and the power spectral density of $T_s(t)$ are presented in Fig. 8.

The annual failure function is written as

$$G_{ULS}(t) = T_{BS}(t) - T_{smax}(t) \quad (11)$$

The annual probability of failure in strength condition is calculated as

$$P_{f_ULS}(t) = P(G_{ULS}(t) < 0) \quad (12)$$

Table 1. The annual breaking tension of chain link at the fairlead connection point considering an annually reduced diameter of $R_{corr} = 0.4$ mm/year.

t (years)	Residual thickness (mm)	Residual MBS (kN)	Mean BS (kN)	Variance of BS
0	95	9.987E3	11.984E3	359.043E3
1	94.6	9.911E3	11.894E3	353.655E3
5	93	9.613E3	11.535E3	332.655E3
8	91.8	9.391E3	11.269E3	317.476E3
10	91	9.244E3	11.093E3	307.624E3
13	89.8	9.025E3	10.830E3	293.243E3
15	89	8.88E3	10.657E3	283.917E3
18	87.8	8.665E3	10.398E3	270.313E3
20	87	8.523E3	10.227E3	261.498E3

Table 2. The annual breaking tension of chain links at the fairlead connection point considering an annually reduced diameter of $R_{corr}=0.8$ mm/year

T (years)	Residual thickness (mm)	Residual MBS (kN)	Mean BS (kN)	Variance of BS
0	95	9.987E3	11.984E3	359.043E3
1	94.2	9.836E3	11.804E3	348.323E3
5	91	9.244E3	11.093E3	307.624E3
8	88.6	8.809E3	10.570E3	279.331E3
10	87	8.522E3	10.227E3	261.498E3
13	84.6	8.101E3	9.721E3	236.246E3

15	83	7.824E3	9.389E3	220.382E3
18	80.6	7.416E3	8.899E3	197.996E3
20	79	7.149E3	8.579E3	183.984E3

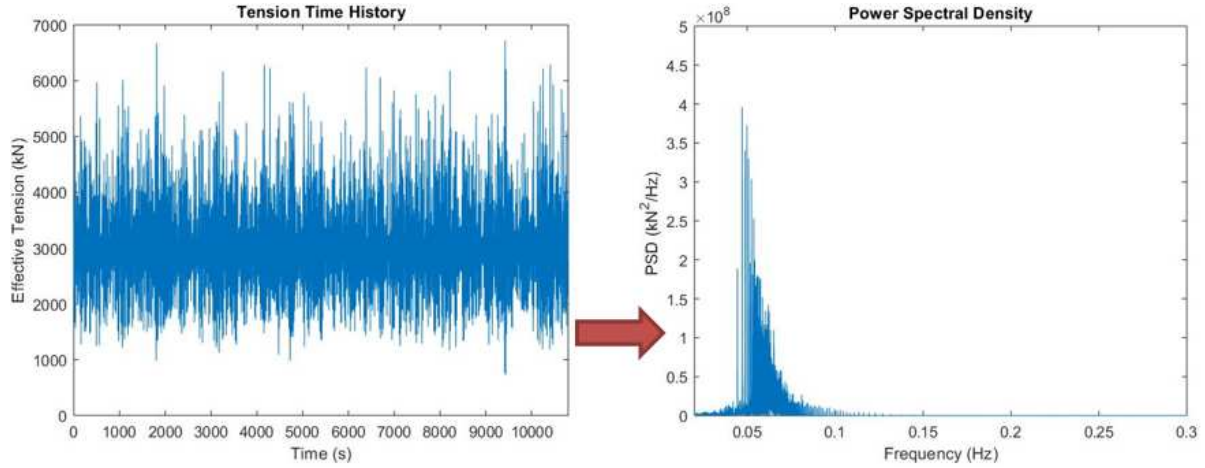


Fig. 8. The time history and PSD of the tension response, $T_s(t)$, in the design extreme sea state.

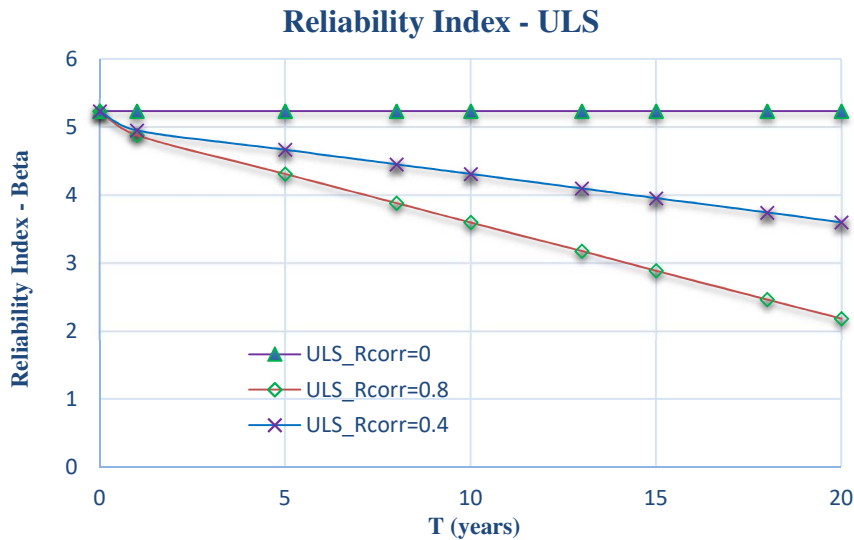


Fig. 9. The annual reliability index in strength condition of the chain link.

The annual reliability index for the 100-years-return-period sea state of the chain link connected to the fairlead are presented in Fig. 9. It can be seen that due to corrosion, the reliability index decreases as the breaking strength of the chain links decreases. Therefore, if corrosion was not considered in the design (e.g. by increasing the diameter of chain links DNV OS-E301 (2011), API-RP-2SK (2005), Bureau Veritas NR 493 (2015), it might be possible that after a few years of deployment, the structure would no longer be able to withstand the initial extreme design sea state. For instances, if we consider that a reliability

index of 3.7 (Schulze, 2006; Vazquez-Hernandez et al., 2006) is acceptable, the mooring line is then considered failed under the same design extreme sea state only after 10 years of deployment with an annually reduced diameter of 0.8 mm/year. In other words, maintenance or replacement of lines should be done after 10 years of operation in order to ensure the safety requirement in extreme condition.

6.2. Reliability in fatigue condition (FLS)

The DNV’s S-N fatigue curve for chain links (DNV OS-E301, 2011) is used to calculate the fatigue damage. The estimated fatigue life is 20 years with a safety factor equals to 3 (Lardier et al., 2008). The objective for this setting is to investigate the added effect of corrosion on the cumulative fatigue damage by calculating the annual reliability index of the chain links taking into account an annually reduced diameter of the chain links (Lardier et al., 2008).

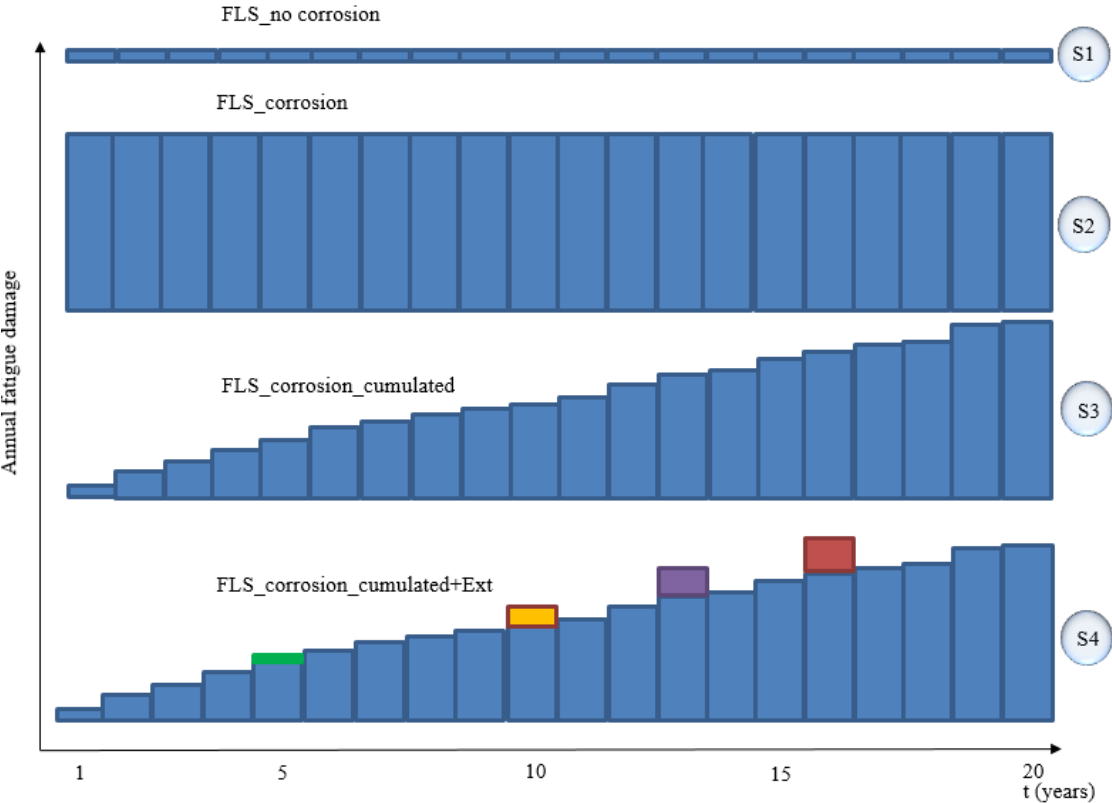


Fig. 10. Different approaches to calculate the fatigue damage of the chain links highlighting the effect of corrosion. The added damage due to extreme sea states (e.g. those with a return period of 10, 20, 50 or 100 years that might probably happen during operation) are illustrated in green, yellow, violet and red color of the case S4.

Fig. 10 illustrates different approaches to calculate the cumulative fatigue damage of the chain links considering the effect of corrosion. S1: the cumulative fatigue damage is calculated without corrosion. S2: the cumulative fatigue damage is calculated with the same quantity of material loss (reduced diameter) for 20 years of deployment. S3: the cumulative fatigue damage is calculated considering an annually reduced diameter of the chain link. S4: the

cumulative fatigue damage is calculated in the same manner as the case S3 but considering also the added fatigue damage due to the design extreme sea states (e.g. with a return period of 100, 50, 20, 10 years, etc.) that might probably happen at any moment within the expected service life of the mooring lines. As mentioned above, the effect of corrosion is considered by reducing the diameter of the chain links and the stiffness of the chain sections. As we consider here an annual reduction of diameter of the chain links, the stress response is considered unchanged within one year for the same sea states, so as the fatigue damage D_{annual} . With a more frequent measurement, the cumulative fatigue damage can be determined by summing the damage calculated from the measured data for every month or couple of months.

The annual probability of failure of the chain links can be calculated as

$$P_{f_FLS} = P(G_{FLS}(t) < 0) \quad (13)$$

where $G_{FLS}(t) = G_{FLS_1}(t) = \Delta - t \cdot D_{\text{annual}}$ for the cases S1 and S2

$$G_{FLS}(t) = G_{FLS_3}(t) = \Delta - \sum_{k=1}^t D_{\text{annual}} \quad \text{for the case S3}$$

$$G_{FLS}(t) = G_{FLS_4}(t) = \Delta - \left(\sum_{k=1}^t D_{\text{annual}} + D_{ULS} \right) \quad \text{for the case S4}$$

Δ is the model uncertainty in the Palmgren-Miner rule, presented in Table 3.

Table 3. The uncertainties of the Palmgren-Miner rule and the DNV's S-N curve (Lardier et al., 2008; Mathisen et al., 2005).

Parameters	Distribution	Mean value	Standard deviation
Model uncertainty in the Palmgren-Miner rules, Δ	Log-normal	1	0.3
K, the S-N curve parameter	Normal	11.55	0.24
m	Fixed	3	-

The annual reliability index of the chain link connected to the fairlead is shown in Fig. 11 and Fig. 12 considering the effect of corrosion as an annually reduced diameter of chain links of 0.4 mm/year and 0.8 mm/year. We observe in the Fig. 11 that the reliability index is more precisely calculated in the case FLS_Rcorr=0.4_Cumulated (case S3) where the fatigue damage is cumulatively summed considering an annual reduction of diameter of chain links. By contrast, the case FLS_Rcorr=0.4 (case S2) seems overly conservative since only the final reduced diameter at the end of the 20 years is considered to calculate the annual damage, then multiplied with the number of operating year. With the assumption that there might be a 100-year-return-period sea state happen during the structure life time, the reliability index is

calculated as FLS_Rcorr=0.4_Cumulated+Ext (case S4) by summing the added damage caused by this extreme sea state into the cumulative fatigue damage. The added damage caused by the extreme sea states is an interesting view that allows forecasting the level reliability of structures in FLS considering disadvantage scenarios such as extreme sea states. This can be useful to inform stake-holders about the probable risk and to support decisions regarding maintenance or replacement of lines.

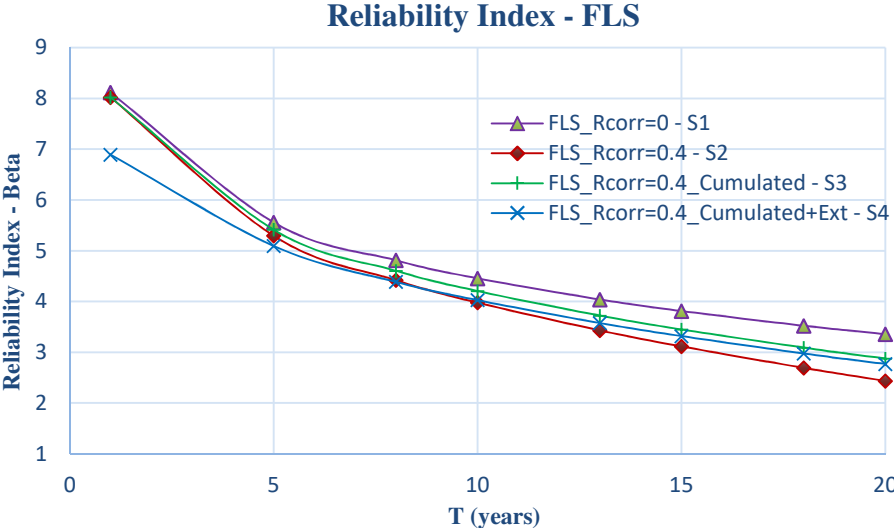


Fig. 11. The annual reliability index calculated based on different approaches, Rcorr = 0.4 mm/year.

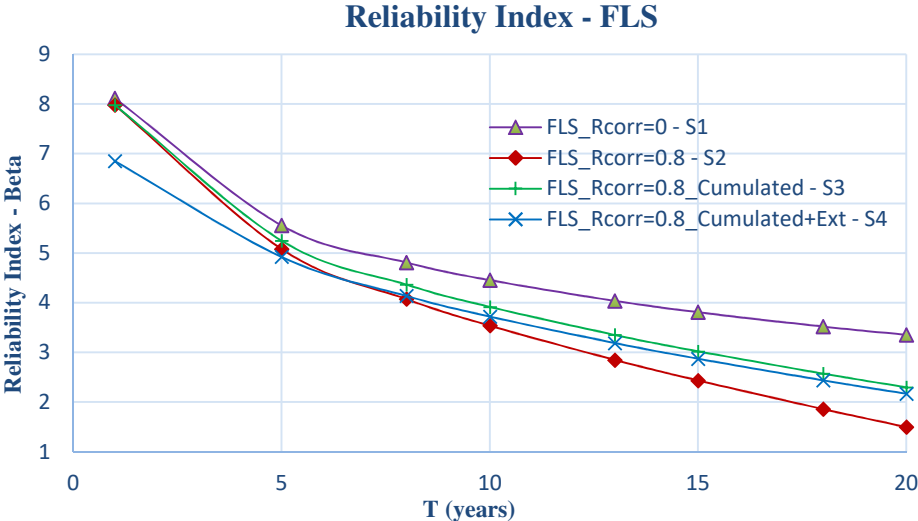


Fig. 12. The annual reliability index calculated based on different approaches, Rcorr = 0.8 mm/year.

Again, if a reliability index that is not smaller than 3.7 is considered acceptable, the fatigue life of the chain link should be 15 years for the case S3 and 13 years for the case S4 instead of 20 years where the corrosion effect is not considered. As a consequence, maintenance or line replacement should be done after 13 or 15 year of deployment. In other words, the safety coefficient should not be smaller than 4 for the case S3 and 5 for the case S4 in order to ensure a design fatigue life of 20 years. It's also worth noting that the recommended design

safety factor for the SN curve given by DNV OS E301 (2011) is 5. Similar conclusions can be made for the Fig. 12 where we consider an annually reduced diameter of the chain links of 8 mm/year.

7 Conclusions and discussion

This study discusses a methodology for modelling and fatigue life monitoring of mooring lines for FWTs based on the reliability approach. In our opinion, in order to understand why and how mooring responses are modified during deployment, it is crucial to identify and account for not only the likely modified mechanical behaviors of mooring components but also the marine environmental factors influencing such behaviors. Therefore, we believe that in addition to the current monitoring approach (Thies et al., 2014) that uses directly field measurement to calculate fatigue damage, the numerical modeling should also be implemented in parallel, in a so-called digital twin. Indeed, once the present modeling and fatigue life monitoring is properly implemented, not only the actual state of the mooring lines will be better controlled but also the cumulative fatigue will be better estimated. As a consequence, the actual level of reliability of the system can be better predicted by fully understanding the actual state of mooring lines such as, residual breaking strength and stiffness of mooring components, better estimating the cumulative fatigue damage and better forecasting disadvantage events (e.g. added damage due to extreme sea states). Based on that, decisions regarding maintenance or replacement of lines can be made once the estimated reliability level reaches the critical value. Furthermore, to us, a comprehensive methodology for modeling and life monitoring also means a closer look on the design safety factor, and as a consequence, a more cost-effective design. Finally, the authors believe that different safety factors should be considered for MRE devices since those are unmanned structures that are significantly different from Oil and Gaz platforms where the consequences for environment, economy and human losses are not in the same order of magnitude. Moreover, since maintenance or replacement of lines for MRE devices in shallow water seems simpler and less costly than for deep-water O&G platforms, the present methodology suggests that those could be the other potential options rather than an over-engineered mooring system which would not be commercially viable for large scale deployments. Otherwise, a common failure mode is from poor quality assurance/ quality control (QA/QC) in manufacturing or damage from handling and installation that this methodology does not address. Extending the methodology to consider these issues is then among the authors' future scope of work.

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