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Evaluation of ship structure reliability during design, maintenance, and repair phases

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Abstract

In the present study, the reliability evaluation application during design, maintenance and repair phases have been investigated for the girder of a ship's hull. The objective of the project was to develop reliability-based methods which are to be used for the design of ship structures, in particular by the calibration of the safety factors in the design rules. In order to evaluate the structural strength, the extended model of the ultimate limit state of the hull-girder, regarding corrosion and fatigue defects, has been used based using a time-dependent probabilistic analysis. Time-dependent reliability has been evaluated using the required minimum elastic section modulus; in the case of fatigue in a ship's deck this process has been done using mechanical fracture and the S-N curve. The results from the reliability evaluation using the Monte-Carlo simulation method and First-order reliability methods (FORM), indicated that these two methods agreed well. Analysis of the corrosion defect reliability showed a decrease of the structure's reliability during its lifetime; hence it is possible to use the reliability criteria in the design phase in order to achieve a better perception of the structure's operation during its lifetime with regard to environmental conditions. A comparison between the fatigue analysis results showed that the fracture mechanics method gave more conservative results compared to the S-N curve method, because of the way it considers early crack size.

Introduction

Risk analysis is the main factor in studying the fast development of the effect of some natural events in marine industries. It is used widely in the air-space, nuclear, and chemical industries and also as a supplementary method in marine industries. The International Maritime Organization (IMO) has presented main instructions for risk analysis that are used specially for marine zones (Embankment, 2002). It is called the Formal Safety Assessment (FSA). This evaluation is based on accepted principles of error and danger analysis processes. The IMO has offered a new method for the main reliability and also high efficiency of the main hull girder of a ship's structure (Hørte et al., 2007).

Risk analysis is a systematic and logical process, the goal of which is increasing marine security and marine environmental protection in order to decrease potential dangers. This process is a certain method that protects health, life, and environmental properties according to ship classification roles. Each method of risk analysis is suitable for certain stages of a system's life cycle (Ayyub et al., 2002).

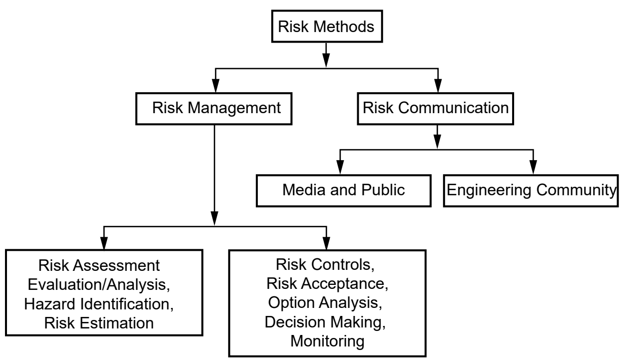

Figure 1. Classification of risk methods (Ayyub et al., 2002)

The IMO and the International Association of Classification Societies (IACS) are developing advanced and complicated roles in order to establish structural principles. According to the development the structural reliability analysis is the best method for code calibration. Corrosion and fatigue cracks are pervasive defects in ship's hull. If each fracture mode isn't completely monitored then it can lead to a catastrophic fracture or a complete stop. The operational probabilistic analysis is used in order to evaluate the ship's strength and operational process.

The evaluation of ship structure reliability

The reliability evaluation for the total limit state of the main hull girder is used for better design and fatigue limit states and is applied to maintenance and repair programs. One of the destructive mechanisms in a ship is the corrosion of the ship's hull. The investigation of a hull's risk needs to determine the strength of the limit state function with reference to the hull's structure. The location of the ship's hull girder for study is the amidship section. The governing limit state mode for ultimate strength can be calculated by the following equation (Akpan et al., 2002):

$$g(t) = M_u(t) - M_L(t) \tag{1}$$

where: $M_u(t)$ is the ultimate hull girder moment capacity, as determined by the critical stress of the respective failure mode and effective section modulus, and $M_L(t)$ is the external load's effect on the ship's structure. Both $M_u(t)$ and $M_L(t)$ are random variables and may assume several values. The following events or conditions describe the possible states of the girder (Ayyub, Akpan et al., 2002):

1. $g(t) < 0 \rightarrow$ The structure has failed. This represents a failure state since this means that $M_L(t)$ exceeds $M_u(t)$;
2. $g(t) = 0 \rightarrow$ Represents the limit state surface or border surface between the safe state and the failure state. The boundary between these two regions is the failure surface;
3. $g(t) > 0 \rightarrow$ The structure is in a safe state. Hence, the associated parameter region is referred to as the safe domain. This represents the safe state.

The aging of the ship's structure overtime results in the decrease of its final strength capacity. The equation (1) specified the vertical bending moment parameters, such as hull bending. The final bending moment capacity of the hull girder is calculated by equation (2) (Akpan et al., 2002):

$$M_u(t) = \phi \sigma_u Z(t) \tag{2}$$

The non-dimensional factor ϕ is known as the buckling knock down factor, σ_u is the ultimate strength of the ship's hull cross section, and $Z(t)$ is the elastic modulus of the midship hull section. In cases where a relationship between the damage, such as fatigue cracks and corrosion, and σ_u can be established, σ_u should be replaced with that relationship. It is well known that structural degradations that will affect the hull girder's capacity by reducing the section modulus $Z(t)$ change over time.

Corrosion decreases the section modulus of the ship's hull structure by reducing the thickness of primary structural members and it also reduces the ability of the structure to resist the externally induced bending moments. Several models of general corrosion growth have been suggested (Paik, Kim & Lee, 1998; Orisamolu, Akpan & Brennan, 1999; Orisamolu, Brennan & Akpan, 1999; Orisamolu, Lou & Lichodziejewski, 1999). The current model used in this paper is the equation (3) (Akpan et al., 2002):

$$r(t) = C_1 (t - t_0)^{C_2} \tag{3}$$

where $r(t)$ is rate at which the thickness is decreasing, t_0 is the life of the coating in years, t is the age of the vessel in years, C_1 and C_2 are coefficients of random variables. C_1 represents the annual corrosion rate and C_2 can take values ranging from 1/3 to 1. The life of the coating varies for different vessels and depends on the coating type (Akpan et al., 2002). Thus the moment capacity is given by:

$$M_u(t) = \phi \sigma_u \begin{bmatrix} Z(r(t_0)) & t \le t_0 & r(t_0) = 0 \\ Z(r(t)) & t \le t_0 & r(t) = 0 \end{bmatrix} \tag{4}$$

According to the equation (4), $Z(t)$ is presented as data collected, the prediction equation for the reduction of the hull's girder section module compared to the initial values have been shown in Table 1.

Table 1. The equations for predicting the mean values and standard deviations of hull girder section module loss (Câmara & Cyrino, 2012)

Value	Reducing of hull girder section module	Years
Mean	$R_m(t) = \frac{0.62(t-6.5)^{0.67}}{100}$	$t > 6.5$
Mean + standard deviation	$R_{m+\sigma}(t) = \frac{0.8(t-5)^{0.75}}{100}$	$t > 5$
Standard deviation	$R_{m+\sigma} = R_{m+\sigma}(t) - R_m(t)$	$t > 6.5$

The replacement of the mean value equation plus the standard deviation in equation (4) resulted in the main hull girder bending moment capacity.

$$M_u(t) = x_u \phi \sigma_u S_m \left[1 - \left(0.8 \frac{(t-5)^{0.75}}{100} \right) \right] \quad (5)$$

where x_u is the random variable representing the modeling uncertainty in ultimate strength, and S_m is the main or initial hull girder bending moment capacity. The external load's effect model over the ship structure is equal to the equation (6):

$$M_L(t) = x_{sw} M_{sw}(t) - x_w x_s M_w(t) \quad (6)$$

The primary total bending moment on the hull can be decomposed into two components: the still-water bending moment (M_{sw}) and the wave-induced bending moment (M_w). x_{sw} is the random variable representing the modeling uncertainty in the still-water bending moment, x_w is the modeling uncertainty in the wave bending moment, and x_s is a model that accounts for non-linearity in the wave bending moment (Akpan et al., 2002). The distribution of model uncertainty and parameters has been given in Table 2 (Mansour & Hovem, 1994).

Table 2. Distributions of model uncertainty and parameters (Mansour & Hovem, 1994)

Random variable	Distribution	Mean	Coefficient of variation (COV)
x_u	Normal	1.0	0.15
x_{sw}	Normal	1.0	0.05
x_w	Normal	0.9	0.15
x_s	Normal	1.15	0.03

Mansour and Wirsching applied a simple linear model (Mansour & Wirsching, 1995). Furthermore $M_{sw}(t)$ and $M_w(t)$ are dependent on time, but in this study they are independent of time because of the simplification in the proposed method. According to previous studies, many researchers applied equations presented by the IACS and in this study the same equations were also applied (Fang & Das, 2005).

$$\begin{cases} M_{sw} = -0.065 C_w L^2 B (C_B + 0.7) & \text{sagging} \\ M_{sw} = C_w L^2 B (0.1225 - 0.015 C_B) & \text{hogging} \\ M_w = -0.11 C_w L^2 (C_B + 0.7) & \text{sagging} \\ M_w = 0.19 C_w L^2 B C_B & \text{hogging} \end{cases} \quad (7)$$

in which L, B, and C_B are the ship length, breadth, and block coefficient respectively, and C_w is the wave coefficient given by (Fang & Das, 2005):

$$C_w = \begin{cases} 10.75 - \left(\frac{300 - L}{100} \right)^{\frac{3}{2}} & 100 < L < 300 \\ 10.75 & 300 < L < 350 \\ 10.75 - \left(\frac{L - 350}{150} \right)^{\frac{3}{2}} & 350 < L \end{cases} \quad (8)$$

By the substitution of equation (5) and (6) into equation (1), the following statement was obtained for the limit state equation (Fang & Das, 2005).

$$g(t) = x_u \phi \sigma_u Z(t) - (x_{sw} M_{sw} + x_w x_s M_w) \quad (9)$$

$$g(t) = x_u \phi \sigma_u S_m \left[1 - \left(0.8 \frac{(t-5)^{0.75}}{100} \right) \right] + \\ - (x_{sw} M_{sw} + x_w x_s M_w)$$

The probabilistic characterization of model uncertainty and the principal random variables was presented by Akpan et al. and has been shown in Table 3 (Akpan et al., 2002).

Table 3. Probabilistic characterization of model uncertainty and the principal random variables (Akpan et al., 2002)

Variable	Mean	Coefficient of variation (COV)	Distribution type
x_u	1	0.15	Normal
x_{sw}	1	0.05	Normal
x_w	0.9	0.15	Normal
x_s	1.15	0.03	Normal
σ_u	μ	0.1	Log-normal
M_{sw}	μ	0.4	Log-normal
M_w	μ	0.1	Extreme
ϕ	0.95	–	Constant

Modeling the effect of fatigue cracks

The presence of a fatigue crack can lead to the loss of effectiveness of a structural element when the crack reaches a critical size. Thus, the net section modulus that resists longitudinal loads is reduced. The reduction may be in such a way as to increase the nominal stress levels within the amidships, which in turn increases the rate of crack growth. The two main approaches for assessing fatigue strength are (Akpan et al., 2002):

1. The S-N curve for crack-initiation evaluation;
2. The fracture mechanics approach for crack-propagation evaluation.

The S-N curve for crack-initiation evaluation

The S-N curve is the graphical presentation of the dependence of the fatigue life (N) on the fatigue strength (S) (DNV, 2014). The S-N curve method is based on strength and crack initiation and is one of the critical parts of the structure which is explored as a function of many stress cycles. The fatigue life under varying loading is calculated based on the S-N fatigue approach under the assumption of linear cumulative damage (Palmgren-Miner's rule) (DNV, 2014). The S-N curve is specified by material type, structure type (welding geometry, direction and qualify), and environmental condition (cathodic protection or air corrosion) and also the linear regression analysis result for a certain reliability domain.

$$\Delta S = \left(\frac{K}{N}\right)^{\frac{1}{m_f}} \tag{10}$$

where ΔS is the stress range, N is number of cycles to failure, m_f is the inverse slope of the S-N curve and K is determined from the S-N curve by:

$$\log K = \log a - 2\sigma_{\log N} \tag{11}$$

where a is a constant referring to the mean S-N curve and $\sigma_{\log N}$ is the standard deviation of $\log N$ (Mansour & Hovem, 1994). Fatigue damage is defined by the following equation (Beghin, 2006):

$$D = \frac{N_s}{K} E\left[\Delta S^{m_f}\right] \tag{12}$$

where N_s is the cycle in the performance period and $E[\Delta S^{m_f}]$ is the expected value, or mean value of ΔS. The probabilistic density function for a long time stress domain is evaluated by the local load with the Weibull distribution:

$$f(\Delta S) = \frac{\zeta}{W}\left(\frac{\Delta S}{W}\right)^{\zeta-1} \exp\left(-\frac{\Delta S}{W}\right)^{\zeta} \tag{13}$$

where ζ is the Weibull distribution shape factor, W is the Weibull factor, and ΔS is the stress range. For a one-slope S-N curve, the cumulative damage ratio is evaluated by the following equation (Beghin, 2006):

$$D = \frac{N_s}{K} W^{m_f} \Gamma\left(1 + \frac{m_f}{\zeta}\right) \tag{14}$$

where Γ is the Gamma function given by $\Gamma(a+1) = \int_0^\infty t^a e^{-t}\,\mathrm{d}t$ and W is the characteristic value of the Weibull distribution given by:

$$W = \frac{S_R}{(\ln N_R)^{1/k}} \tag{15}$$

where S_R is the stress range with the probability of $1/N_R$, N_R is the number of cycles corresponding to the probability of the exceedance of $1/N$ and k is the Weibull shape parameter. According to equation (14), the limit state function for the reliability evaluation based on the S-N curve is given by:

$$g(t) = \Delta - D(t) \tag{16}$$

$$g(t) = \Delta - \frac{vt}{K} W^{m_f} \Gamma\left(1 + \frac{m_f}{\zeta}\right)$$

where t is time and constant, and Δ is the cumulative damage amount according to the failure fracture and is variable and it may also be modeled with the Normal-Log distribution, $D(t)$ is the fatigue cumulative damage amount with respect to the time (t) and $v = (S_Q/S_R)^k \ln N_R$ where S_Q is the stress range at the intersection of the two segments of the S-N curve.

The fracture mechanics approach for crack-propagation evaluation

The fracture mechanics approach can be used in risk analysis based on the crack-propagation evaluation. The fracture mechanics approach uses crack-growth equations to predict the size of a crack as a function of time. The mechanistic model relates the crack growth to the stress intensity factor, stress range, material, and environmental properties. Many proposals have been made for predicting crack growth. The most commonly used mechanistic model is the Paris–Erdegen formula given by (Akpan et al., 2002):

$$\frac{\mathrm{d}\alpha}{\mathrm{d}N} = C(\Delta K)^m \tag{17}$$

$$\Delta K = Y(\alpha)\Delta S_m \sqrt{\pi a} \rightarrow \Delta S_m = \frac{\Delta S}{k_f} \tag{18}$$

where α is the crack size, N is the number of load cycles, $\mathrm{d}\alpha/\mathrm{d}N$ is the crack growth rate or crack growth per cycle ranging from 10^{-3} to 10^{-6} mm/cycle for most marine load cases of interest, C and m are constant amounts related to material and testing conditions (stress ratio, environment), and determined experimentally, ΔS_m is the double stress range in the top of the crack, k_f is the stress intensive factor according to welding geometry, ΔK is the stress intensity factor and $Y(\alpha)$ is a geometric factor. After simplification and integration of the equation (18) the following can be presented (DNV, 2015):

$$\int_{a_0}^{a(t)} \frac{\mathrm{d}a}{\left(Y(a)\sqrt{\pi a}\right)^m} = CNE\left(\Delta S_m\right)^m \tag{19}$$

The reliability model of mechanical fracture is based on 1D and the limit state equation given by:

$$g(t) = \int_{a_0}^{a} \frac{\mathrm{d}a}{\left(\varepsilon_y Y(a)\sqrt{\pi a}\right)^m} + -C\left[vTp\varepsilon_s\left[\left(\frac{W}{k_f}\right)^m \Gamma\left(\frac{m}{\zeta}+1\right)\right]\right] \quad (20)$$

where α_0 is depth of the initial crack, α is depth of the final crack, $Y(\alpha)$ is geometrical factor, W is the Weibull distribution scale factor, ζ is the Weibull distribution shape factor, ε_y is the coefficient of uncertainty geometry parameters of $Y(\alpha)$, ε_s is the coefficient of uncertainty stress evaluation and k_f is the coefficient of stress concentration.

Reliability Evaluation

Monte-Carlo Simulation Method (MCS)

The most applicable simulation method is the Monte-Carlo Simulation method which was presented by Metropolis and Ulam in 1949 (Metropolis & Ulam, 1949). The solution method by the use of facture probability based on the Monte-Carlo simulation is as follows:

$$P_F = \frac{n_f}{n} \quad (21)$$

Therefore in this method, the fracture probability is the ratio of the number fracture section nodes n_f to all the nodes that are produced by the density function variable n. Using this method is very difficult for evaluating the small amount of fracture probability because producing huge models is very time-consuming.

First-Order Reliability Method (FORM)

Hasofer and Lind presented one of the first effective methods for reliability evaluation (Hasofer & Lind, 1974). In the First-order reliability method, the failure surface $g(u) = 0$ at the design point U^* is approximated by the hyperplane normal to the vector U^* (Hasofer & Lind, 1974; Rackwitz & Flessler, 1978; Hohenbichler & Rackwitz, 1981; Vrouwenvelder & Karadeniz, 2006). By using the Taylor expansion the failure function, $Z = g(u)$ is linearized and after the linearization it can be stated as (Vrouwenvelder & Karadeniz, 2006):

$$Z = g\left(u^*\right) + \nabla g\left(u^*\right)^T\left(\mu_U - U^*\right) \quad (22)$$

where $\nabla g(u^*)$ is the gradient vector at the design point U^*. Since U is a normal vector the linearized failure function given by the equation (22) will also have a normal distribution. The mean value μ_z and the variance σ_z^2 of this linear random function are calculated in general at the design point U^* as stated by (Vrouwenvelder & Karadeniz, 2006):

$$\mu_z = g\left(u^*\right) + \nabla g\left(u^*\right)^T\left(\mu_U - U^*\right) \rightarrow \mu_z = g\left(\mu_U\right) \quad (23a)$$

$$\sigma_z^2 = \nabla g\left(u^*\right)^T C_U g\left(u^*\right) \rightarrow \sigma_z = \left[\nabla g\left(\mu_U\right)^T C_U g\left(\mu_U\right)\right]^{\frac{1}{2}} \quad (23b)$$

The mean value approach is based on the assumption that the linearization of the failure function is carried out at the mean values of the design variables, i.e. $U^* = \mu_U$. In the equation (23), the superscript T denotes a transpose (here a row vector), μ_U is the vector of mean values of the random variables, and C_U is the matrix of their covariances, which is a diagonal matrix. Having determined the mean value and the standard deviation of the failure function Z which has an approximately normal distribution given by (Vrouwenvelder & Karadeniz, 2006):

$$u = \frac{z - \mu_z}{\sigma_z} \quad (24)$$

In which the failure domain is defined when $u \leq -\beta$, i.e. ($z \leq 0$), where β is called the reliability index given by (Vrouwenvelder & Karadeniz, 2006):

$$\beta = \frac{\mu_z}{\sigma_z} \quad (25)$$

Then, the failure probability is simply calculated from (Vrouwenvelder & Karadeniz, 2006):

$$P_f = \Phi(-\beta) \quad (26)$$

where Φ( ) is the standard normal distribution function. Based on the First Order Reliability Methods several approaches are possible. In the advanced reliability methods, the linearization of the failure function is performed on the failure surface and its mean value can be stated from the equation (23) as (Vrouwenvelder & Karadeniz, 2006):

$$Z = \nabla g\left(\mu_U\right)^T\left(U - U^*\right) \quad (27a)$$

$$\mu_z = \nabla g\left(\mu_U\right)^T\left(\mu_U - U^*\right) \quad (27b)$$

Since the term $g(u^*)$ will be zero on the failure surface. The problem here is to find the unknown design point on the failure like U^*, which can be obtained by using an iterative algorithm which was presented by Rackwitz (Rackwitz, 1975). For this purpose, the standard deviation of the failure independent normal random variables U, are represented by the diagonal matrix σ_U. It is obvious that the relation between the diagonal matrices of standard deviations and covariance's, σ_U and C_U, is simply expressed as (Vrouwenvelder & Karadeniz, 2006):

$$\sigma_U = \sqrt{C_U} \tag{28}$$

Target reliability

The modes of failure of ship hull girders have serious consequences such as the entire loss of the ship, loss of lives, and environmental damage. Accordingly, the second method seems to be the correct one to be adopted for selecting target reliability levels since there is a lot of data available from the currently used design codes that resulted in structures of adequate reliability. The recommended range of target reliability indices for hull girder bending is set to be from 4.0 to 5.0 for a sagging condition, and 5.0 to 6.0 for a hogging condition for naval ships (Mansour et al., 1996). Mansour presented the reliability index for strength against collapse for commercial vessels by studying research on target reliability (Mansour et al., 1997) (Table 4).

Table 4. Target reliability index that was presented by Mansour (Mansour et al., 1997)

Limit state	Commercial ships	Naval ships
Ultimate	3.5	4.0
Secondary	2.5	3.0
Tertiary	2.0	3.0

The recommended target safety indices have been summarized in Table 5. These are lifetime values that are used to derive partial safety factors in this prototype code.

Table 5. Recommended Target Safety Indices Relative to the Service Life of Ships (Mansour et al., 1996)

	Tanker β_0	Cruiser β_0
Hull girder collapse	4	5
Hull girder initial yield	4.5	5.5
Unstiffened panel	3	3.5
Stiffened panel	3.5	4
Fatigue		
Group 1 (Not Serious)	2.0	2.5
Group 2 (Serious)	2.5	3.0
Group 3 (Very Serious)	3.0	3.5

Case study

Corrosion reliability evaluation of a ship hull girder

The corrosion reliability evaluation of a ship hull girder was done for a long time. The reliability result was evaluated by the Monte-Carlo simulation method and the First-order reliability method and used the limit state equation (9) for a ship hull girder. The required section module was in accordance with the ABS and is described as follows:

$$S_M = \frac{M_t}{f_p} \tag{29}$$

where M_t represents the total bending moment, which is a combination of the still-water bending moment M_{sw} and the wave-induced bending moment M_w and f_p is the nominal compressive stress. The dimensions of the ship LNG_QFLEX have been presented in Table 6 (Câmara & Cyrino, 2012) and the amount of related variables has been defined in Table 7 (Câmara & Cyrino, 2012).

Table 6. Dimensions of the ship LNG_QFLEX (Câmara & Cyrino, 2012)

Number	Dimension	Amount
1	Length	303 m
2	Breath	50 m
3	Block coefficient	0.854
4	Section module	79.13 m^3

Table 7. Characteristics of the applicable variables in the reliability evaluation (Câmara & Cyrino, 2012)

Coefficient of variation (COV)	Mean value	Variable	Distribution type
0.15	1	x_u	normal
0.05	1	x_{sw}	normal
0.15	0.9	x_w	normal
0.03	1.15	x_s	normal
0.1	281 MPa	σ_u	log-normal
0.40	3248 MNm	M_{sw}	log-normal
0.1	3248 MNm	M_w	Gambell
–	0.95	ϕ	Constant

In order to find the point of convergence in the Monte-Carlo method results, there were many analyses with different random samples (Figure 2).

According to Figure 2, it was observed that from the result of the Monte-Carlo method, convergence was reached after 10000 samples, and after this number of samples it was very rare to see a change in the reliability index. The results of the above-mentioned method have been presented in Figure 3. The reliability evaluation was done over a 40 year period. The evaluation of the Monte-Carlo method was done in a 5 year period because it is a time consuming analysis, but this evaluation for the First-Order reliability method was done in a 1 year period.

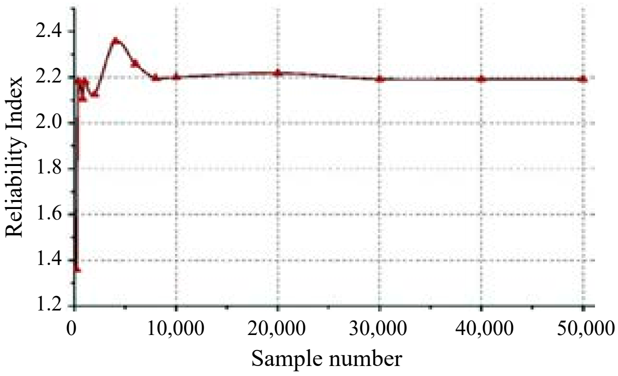

Figure 2. Sensitivity analysis according to many produced samples in the Monte-Carlo method

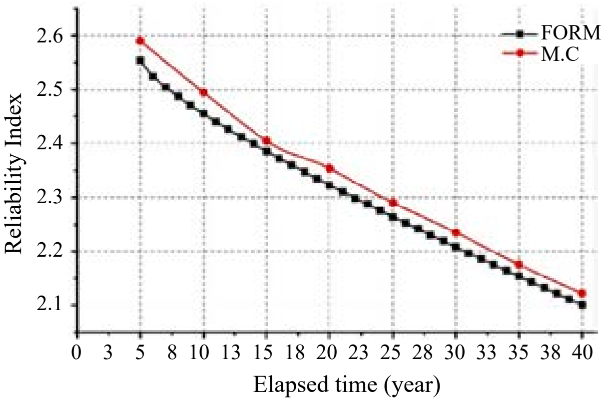

Figure 3. Reliability index according to structure lifetime

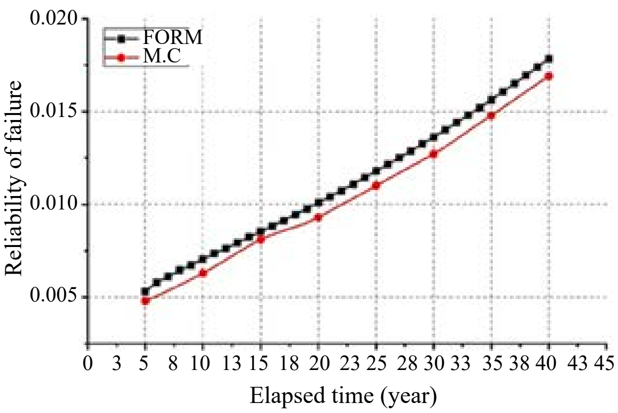

Figure 4. The possibility of obtained fracture from FORM and MCS

According to the curve presented in Figure 3, there is little difference between the two methods and generally in order to decrease the time the analysis takes, the Monte-Carlo method was used as a criterion for the confirmation of the results. The reliability index decreased with the time spent and the development of corrosion and a decrease of the structure section module. The possibility of fracture evaluation has been presented by two methods in Figure 4.

According to Figure 4, the methods have little difference. One of the most important reliability evaluation problems in engineering, which industries must consider, is the determination of the detection period, repair, and maintenance planning. It is defined by the target reliability in a different period according to the structure performance.

Reliability evaluation of a welding part in a ship's structure

Reliability evaluation depends on time with the application of the S-N curve

Machado presented the specified zone according to the planning process of the joint point of the saddle to deck structure in a Floating Production Storage and Offloading unit (FPSO) (Machado, 2002). The saddles lead to stress concentration according to non-uniformity that was caused in the structure. When cyclic loading was carried out it can cause fatigue cracks near welding points between the saddle and the deck surface (Figure 5).

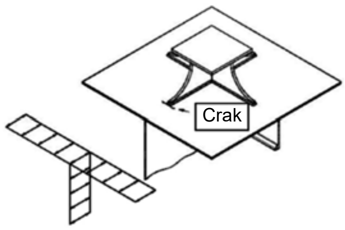

Figure 5. The joint point of the Saddle to the deck and fatigue crack outbreak (Machado, 2002)

According to DNV roles in the evaluation of ship structure fatigue, the mean frequency of the stress cycle is calculated by $4\log(L)$ (DNV, 2016).Where L is the ship length and the mean cycle frequency is equal to $1/p$.

The effective lifetime is considered equal to 20 years in the design phase. According to the presented quantity the Weibull distribution scale factor was equal to 17.271. The mean value and standard deviation variables have been shown in Table 8. The limit state function that was used in this analysis is equation (16).

Table 8. Variables statistical specification

Variables	Mean value	Standard deviation	Distribution type
Δ	1	0.3	Log-normal
K	1E12.992	1E0.2	Normal
W	17.271	5.181	Normal
v	3.13E6	–	Constant
m	3	–	Constant
ζ	0.85	–	Constant

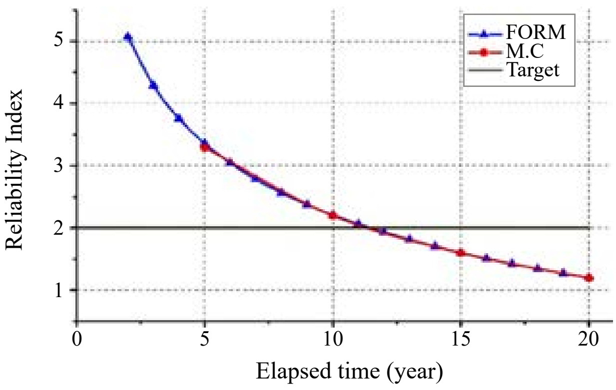

Figure 6. Reliability evaluation index by the Monte-Carlo and the First-order reliability method

According to SSC-392, the crack created at a specified part and the process plan has a target reliability index which is equal to 2. This target index can be a criterion for maintenance and repair planning. Figure 6 has shown the reliability index that was obtained from the two methods in consecutive years.

Figure 6 has shown that there was little difference between the two methods in the reliability evaluation index. Therefore, according to the target reliability index, it was observed that based on the crack propagation analysis in the 11th year the value of the calculated reliability index was less than the target index. This means that strategies for protection and security, such as repair or substitution, are needed in the 11th year.

The reliability evaluation depends on the time of crack propagation with the application of the fracture mechanics approach

The crack reliability evaluation that was described in the above-mentioned section was done with the use of the fracture mechanics approach. For the limit state function the equation (20) was applied. The thickness of the ship's deck surface in the joint with the saddle was equal to 25.4 mm. The critical crack size was considered to be the same as the thickness size. The geometry factor $Y(a)$ was equal to 1.2 and the stress concentration coefficient k_f was equal to 1.5 and had been considered by the DNV (DNV, 2016). The initial size of the crack (a_0) was equal to 0.03 mm with a log-normal distribution and with a variable coefficient that was equal to 0.1. The Paris equation constant parameters for the deck metal material was $m = 3$, and $C = 5.21 \cdot 10^{-13}$. The statistical values of the variables used have been presented in Table 9. The evaluation reliability was done with the two above-mentioned methods and the reliability index result has been shown in Figure 7.

Table 9. Statistical specification of the variables

Variable	Mean value	Coefficient of variation	Distribution type
a_0	0.03	0.1	Log-normal
W	17.271	0.3	Normal
C	5.21E–13	2.44E–13	Normal
$Y(a)$	1.2	–	Constant
m	3	–	Constant
ζ	0.85	–	Constant
ε_y	1	0.1	Normal
ε_s	1	0.1	Normal

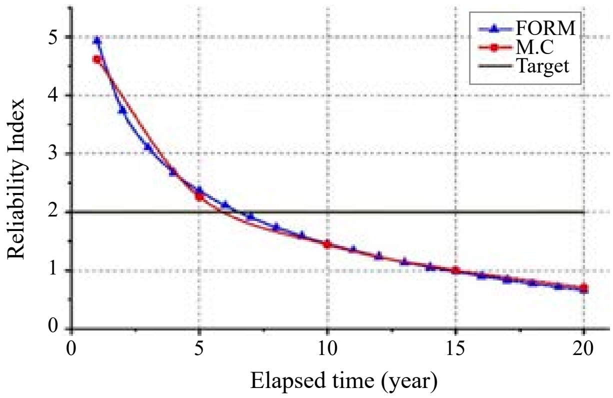

Figure 7. Reliability evaluation index using the Monte-Carlo and the First-order reliability method

The curve presented in Figure 7 has shown the adaption of the results of the Monte-Carlo method and the First-order reliability method. It can be seen at the upper section of the curve that as time passes, the reliability index decreased with a fast slope. Figure 8 has shown the comparison result that was presented by fracture mechanics and the S-N curve.

The results presented in Figure 8 have shown that the probability of fracture with the fracture mechanics approach was more than the probability of fracture with the S-N curve. The reason for this difference is the way the size of the initial crack is considered in the fracture mechanics approach.

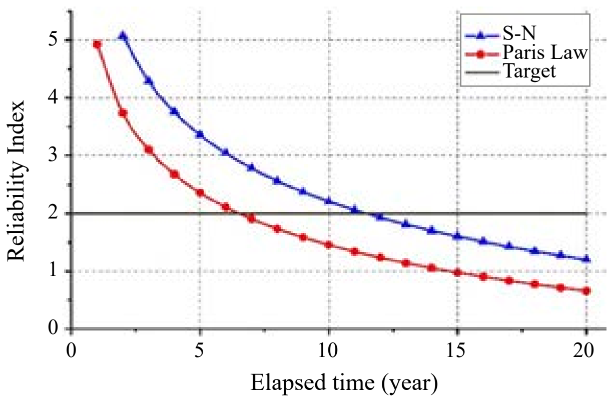

Figure 8. The comparison of the reliability index that was used by the fracture mechanics approach and the S-N curve

The utilization of the fracture mechanics approach in the repair and maintenance phases has precise and conservative results compared to the S-N curve.

Conclusions

A reliability analysis has been performed at 20-year utilization periods. The selection of the target safety index was not an easy task since several factors should be considered, for instance, failure consequences and implied safety levels in present design practice, as well as political, economic, and social factors. Calculation of the target safety index requires the risk analysis to be performed with the above factors taken into account. The reliability evaluation results showed that the first-order reliability method has a short computing time compared to the Monte-Carlo simulation method and the FORM only has a rare difference to the MCS. The ship hull girder analysis showed that the probability approach, due to the consideration of statistical uncertainties, can be a suitable substitution for certain methods which were used in the design phase.

The result of the fatigue reliability evaluation in the ship using two methods, such as the S-N curve and the fracture mechanics approach, showed that the S-N curve can be used in first detection planning due to its indifference to the crack size. Therefore between the two approaches described, the fracture mechanics approach presented more conservative results considering crack size (used in the detection phase) compared to the S-N curve.

References

1. Akpan, U.O., Koko, T.S., Ayyub, B. & Dunbar, T.E. (2002) Risk assessment of aging ship hull structures in the presence of corrosion and fatigue. *Marine structures* 15(3), pp. 211–231.
2. Ayyub, B., Akpan, U., Rushton, P., Koko, T., Ross, J. & Lua J. (2002) *Risk-informed inspection of marine vessels*. SSC-421, Ship Structures Committee, Washington DC.
3. Beghin, D.D. (2006) Fatigue of ship structural details. *The Society of Naval Architects and Marine Engineers* 89.
4. Câmara, M.C. & Cyrino, J.C.R. (2012) *Structural Reliability Applications in Design and Maintenance Planning of Ships Subjected to Fatigue and Corrosion*. ASME 2012 31st International Conference on Ocean, Offshore and Arctic Engineering, American Society of Mechanical Engineers.
5. DNV (2014) *Classification notes No. 30.7: Fatigue assessment of ship structures*. Norway.
6. DNV (2015) *Probabilistic methods for planning of inspection for fatigue cracks in offshore structures*. DNVGL-RP-C210.
7. DNV (2016) *Fatigue Design of Offshore Steel Structures*. DNVGL-RP-C203.
8. Embankment, A. (2002) *Guidelines for formal safety assessment (FSA) for use in the IMO rule-making process*. MSC/Circ 1023.
9. Fang, C. & Das, P.K. (2005). Survivability and reliability of damaged ships after collision and grounding. *Ocean Engineering* 32(3–4), pp. 293–307.
10. Hasofer, A.M. & Lind, N.C. (1974) Exact and invariant second-moment code format. *Journal of the Engineering Mechanics Division* 100(1), pp. 111–121.
11. Hohenbichler, M. & Rackwitz, R. (1981) Non-normal dependent vectors in structural safety. *Journal of the Engineering Mechanics Division* 107(6), pp. 1227–1238.
12. Hørte, T., Skjong, R., Friis-Hansen, P., Teixeira, A. & Viejo de Francisco F. (2007) *Probabilistic methods applied to structural design and rule development*. Proc. of RINA conference on Developments in Classification and International Regulations.
13. Machado, J.M. (2002) *Planejamento baseado em risco de inspeções a fadiga em unidades estacionárias de produção*. Universidade Federal do Rio de Janeiro.
14. Mansour, A. & Hovem, L. (1994) Probability-based ship structural safety analysis. *Journal of Ship Research* 38(4), pp. 329–339.
15. Mansour, A. & Wirsching, P. (1995) Sensitivity factors and their application to marine structures. *Marine Structures* 8(3), pp. 229–255.
16. Mansour, A., Wirsching, P., Luckett, M. & Plumpton, A. (1997) *Assessment of reliability of existing ship structures*. DTIC Document.
17. Mansour, A., Wirsching, P., White, G. & Ayyub, B. (1996) *Probability-Based Ship Design: Implementation of Design Guidelines*.
18. Metropolis, N. & Ulam, S. (1949) The monte carlo method. *Journal of the American statistical association* 44(247), pp. 335–341.
19. Orisamolu, I., Akpan, U. & Brennan, D. (1999) *Application of probabilistic corrosion models for reliability assessment of ship structural panels*. Eighth CF/CRAD Meeting on Naval Application of Materials Technology and Inter-naval Corrosion Conference, Halifax, NS.
20. Orisamolu, I., Brennan, D. & Akpan, U. (1999) *Probabilistic modeling of corroded ship structural panels*. 8th CF/CRAD Meeting on Naval Application of Materials Technology and Inter-Naval Corrosion Conference, Halifax, Nova Scotia, Canada.
21. Orisamolu, I., Lou, X. & Lichodziejewski, M. (1999) *Development of Probabilistic Optimal Strategies for Inspection, Monitoring, Maintenance, Repair and Life Extension*. Tech. Rep. SRV-6-00251, Martec Limited, Halifax, Nova Scotia.
22. Paik, J.K., Kim, S.K. & Lee, S.K. (1998) Probabilistic corrosion rate estimation model for longitudinal strength members of bulk carriers. *Ocean Engineering* 25(10), pp. 837–860.
23. Rackwitz, R. & Flessler, B. (1978) Structural reliability under combined random load sequences. *Computers & Structures* 9(5), pp. 489–494.
24. Rackwitz, R. (1975) *Principles and methods for a practical probabilistic approach to structural safety*. Sub-Committee for First Order Reliability Concepts for Design Codes of the Joint CEBCECM-CIB-FIB-IABSE Committee on Structural Safety.
25. Vrouwenvelder, T. & Karadeniz, H. (2006) *Overview of structural reliability methods*.