Abstracts

Rodney Eatock Taylor

On Modelling the Diffraction of Water Waves
Ship Technology Research 54 (2007), 54-80

The paper reviews features of the diffraction of waves by marine structures (having no forward speed), with an emphasis on the local wave elevation around one or more vertical circular cylinders. Low frequency and high frequency (ray theory) linear approximations are discussed. The pronounced interaction effects in arrays of cylinders at discrete frequencies, associated with the “near-trapping” phenomenon, are illustrated for typical cases of linear, square and rectangular arrays. For each of these cases results are also presented showing the prolonged build-up to trapping after several cycles, following the start of a sinusoidal wave maker in a numerical wave tank. Second-order perturbation theory analysis of diffraction is considered, including application to second-order near-trapping. Fully nonlinear methods of modelling diffraction are briefly reviewed, and links made to limited experimental data on local free-surface effects associated with diffraction.

Keywords: diffraction, offshore structure, seakeeping

Conghong Lu, Yan Lin, Zhuoshang Ji

Ship Hull Representation based on Offset Data with a Single NURBS Surface
Ship Technology Research 54 (2007), 81-88

A method based on a single NURBS surface is presented to represent a ship hull using an existing hull form offset. The method uses a skinning technique with the waterlines and deck side lines as the section curves. The approach is illustrated for two typical ship hulls.

Keywords: CAD, hull design, free form surface

Cesare Mario Rizzo

Application of Reliability Analysis to the Fatigue of Typical Welded Joints of Ships
Ship Technology Research 54 (2007), 89-100

A sensitivity analysis is applied to a reliability model for fatigue life calculations of typical welded joints based on fracture mechanics. The aim is to select the most influencing parameters, before and after inspection updating. A literature survey allowed to provide an overview of the input parameter values, looking at reducing as far as possible the complexity of the numerical model and at identifying the most influencing ones.

Keywords: fatigue, reliability, structural failure
1 Introduction

The principle of fatigue life assessment consists in comparing parameters believed to govern the physical phenomenon with their critical values leading to failure after a given number of cycles. Different approaches can be used and therefore different parameters are believed to govern the phenomenon. Basically two approaches can be distinguished:

- S-N approaches are based on stress analysis of the structure under subject, assuming that fatigue failure occurs at the same stress level and cycles number as that of experimental tests,
- the fracture mechanics (FM) approach assumes an existing crack in the structure growing up to the failure, under specified loading conditions.

Fatigue has become an important consideration in ship structural design, especially since many welded details are repeated in standardized geometries. Classification societies recently introduced explicit fatigue checks in their rules for shipbuilding. Such checks are based on the classic S-N approach, which cannot be easily applied to the condition assessment of structural details during surveys because the fatigue damage level, defined according to the Palmgren-Miner law, is not directly related to the crack growth, i.e. the measurable effect of fatigue. The fracture mechanics approach allows describing the degradation of ageing structures following the evolution of the physical phenomena, at least in the limits of the numerical model. Thus, it could be applied to the condition assessments of ship structures and updated by surveys outcomes. The reliability framework accounts for the intrinsic probabilistic aspects of fatigue and provides a measure of the validity of obtained results. However, fatigue of ships structural details is governed by several parameters: the fracture mechanics model for fatigue life prediction should be feed with appropriate input data. Otherwise results may be worse than the ones of the traditional S-N approach.

The Paris-Erdogan law for fatigue life calculation is a very useful relationship because it covers the range of crack growth rates most useful to metallic welded structures, Paris and Erdogan (1963). Its development was crucial to the adoption of defect-tolerance concepts and to the implementation of damage tolerant design. However, Paris and Erdogan’s formula is based on an approach originally developed for brittle idealized materials. That is not a correct assumption for steel or welded joints, or the ships’ environment.

Many variables affect fatigue. From Burnside et al. (1984) and Nikolaidis and Kaplan (1991) the following classification may be summarized:

- Environmental variables: (electro)-chemical, physical and mechanical properties of sea water and cargo;
- Structures materials: mechanical properties, chemical properties, welding and related micro/macro defects (e.g. residual stress, heat affected zone (HAZ), inclusions, undercutting), etc.
- Acting loads: range, mean level, frequency, density function, from wind, wave, machinery, etc.

The linear fracture mechanics approach offers the following advantages:
It rationally accounts for the influence of flaws on fatigue life, as it measures cracks, i.e. a physical effect, rather than damage / failure, i.e. a conceptual parameter.

It can be used in conjunction with NDT (non-destructive testing) information.

It can be joined with advanced stress analysis techniques.

However, the method is relatively complex and neglects some physical events at the notch, e.g., plasticity, mean stress, cyclic hardening, and softening (unless an advanced theory is used, e.g. elasto-plastic fracture mechanics). Especially, the FM approach requires to assume an initial crack existing in the structural detail: micro-defects always exist in welded details, but their size, location and shape must be correctly defined. Crack growth under variable amplitude loading, under elastic-plastic loading and growth of very small cracks and long cracks in large plates is still a matter for research. While the advantages of FM are attractive, the approach may give bad and non-conservative results if input data are not appropriately set. The work described here is intended to review the uncertainties in the prediction procedure and to provide quantitative information on the reliability of results. The relative importance of uncertainties of the input parameters may be assessed by a sensitivity analysis of numerical models. In order to highlight the difficulties in the application of an engineering tool rather than to present a complex method itself, input values in this paper have been selected by engineering judgment after a literature survey. The model has been simplified as far as possible and uncertainties quantified considering resources available in current design procedures.

2 Fatigue Reliability Model

2.1 Fracture mechanics (FM) approach to fatigue

In linear elastic fracture mechanics, a linear relationship relates the stress intensity factor range $\Delta K$ and the crack growth rate in the range of interest of fatigue analyses:

$$\frac{da}{dN} = C(\Delta K)^m$$

with

$$\Delta K = M_k(a)\Delta\sigma \sqrt{\pi a}$$

(1)

$C$ is the crack growth parameter, $m$ the corresponding slope that characterizes the material. $a$ is the crack depth, $N$ the number of fatigue cycles. $\Delta K$ is the stress intensity factor range. $\Delta\sigma$ represents the acting stress range at the crack tip without considering the crack effects. $M_k$ is a magnification factor that depends on the geometry of the structural detail and on the crack size. The integration of the above equation allows the calculation of the residual fatigue life. Of course limits on the integration in terms of crack size are necessary to obtain the number of cycles at failure. A final crack size may be expressed in terms of the critical size for brittle fracture, or somehow related to geometrical dimensions, generally the plate thickness for through-thickness cracks. It is much more difficult is to define the initial crack size, both for new and old structures.

Eq.(1) represents a simple form of the FM. The $M_k$ coefficient accounts globally for deviations from the ideal case of the infinite cracked plate in uni-axial tension. Several improvements of the procedure, aiming to explicitly account for various effects of the physical phenomenon, are proposed in e.g. Radaj and Sonsino (1999), Pook (2000), Fricke (2003).

In general, material parameters to be used in connection with the Paris law are defined through laboratory testing and, of course, testing conditions should be taken into account when the parameters are applied in calculations. Large uncertainties are caused by:

- lack of data, especially for new materials,
- parameters varying according to environmental conditions,
- inherent defects of welded joints,
crack propagation through different materials in welded joints (i.e. base material and HAZ),
in service conditions not exactly reproduced in laboratories (e.g. environment of tanks).

The material parameters $C$ and $m$ need to be defined carefully, especially in the initiation and final regions of validity of the Paris-Erdogan’s law where respectively micro-mechanics effects and plasticity should be accounted for.

Separation of the variables and integration of Eq.(1) leads to the relationship between the strength vs. the loads in time domain, i.e. the crack size vs. the cycles number:

$$\int_{a_i}^{a_f} \frac{1}{(M_k\sqrt{\pi a})^m} da = \int_{N_i}^{N_f} C \cdot \Delta \sigma^m dN$$  \hspace{1cm} (2)

The index $i$ indicates initial, the index $f$ final. Generally, no fatigue limit (threshold) or stress ratio influence is considered in ship structures because of welding residual stresses, which leads to conservative results because of welding residual stresses. The integration defined in Eq.(2) can be solved analytically only if $M_k$ is assumed independent of the crack size. A surface crack grows approximately in a semi-elliptical shape and the geometry function depends on the crack size, hence Eq (2) needs to be integrated numerically. Since Eq. (1) is valid at any point along the crack front, assuming elliptical shape, two parameters suffice to describe the crack propagation:

$$\frac{da}{dN} = C_a(\Delta K_A)^m \hspace{1cm} \frac{dc}{dN} = C_c(\Delta K_C)^m$$  \hspace{1cm} (3)

$a$ is the crack depth, $c$ the half crack length, $C_a$ and $C_c$ crack growth parameters at the deepest and surface points of the crack front, respectively. The stress intensity factor range $\Delta K$ is correspondingly defined for the deepest and surface points. The material property $m$ depends mainly on the fatigue crack propagation. It is therefore reasonable to assume $m$ to be independent of the crack size, both in the depth and surface directions. Raju and Newman (1981) proposed $C_a = 1.1^mC_c$, thus reducing the two coupled equations to a 1-D model, by fixing the $a/c$ ratio. Alternatively, both equations should be solved coupled.

The stress direction with respect to the crack is also important, even if it can be analytically demonstrated that cracks always propagate in opening mode, i.e. the stress acts perpendicular to the crack propagating axis, at least after a short transition time. Fracture mechanics calculations should therefore be based on maximum principal stress range: implication of combination of shear and bending and of different loads should lead therefore to limited uncertainties. Nevertheless, cracks grow faster for membrane loading than for bending loading, BSI (1999). Actually, the volume of material stressed by the highest stress level is greater for membrane loading. However, the general expression for the stress-intensity factor can be improved defining and substituting in the $\Delta K$ expression:

$$\alpha = \frac{S_{mem}}{S} \hspace{1cm} M_k(a) = M_{k,mem}\alpha + M_{k,bend}(1 - \alpha)$$  \hspace{1cm} (4)

Corrosion is neglected here. Corrosion has two effects on the crack growth phenomena: as the thickness is reduced, the stress level is increased and consequently $\Delta K$ and the crack growth rate. In addition, the crack growth rate is increased because of the chemical actions at the crack tip.

### 2.2 Reliability model

Reliability methods have been identified as a mean for optimising inspection and maintenance costs in offshore structures. Recently, classification societies introduced reliability analyses into their rules for shipbuilding by calibrating scantling formulations. ISO/DIS/18072 standards under development will introduce reliability concepts in the limit state assessment of ship structures, Frieze and Paik (2004). The reliability framework applied to fracture mechanics explicitly quantifies the uncertainties.
of all parameters involved in the failure function as well as the ones deriving from inspections’ updating (i.e. time of inspection and probability of detection of cracks). The reliability model applied here is summarized in the following. Further details may be found in Madsen et al. (1986), Melchers (1999). The safety margin (i.e. failure probability before time \( t \)) is expressed as:

\[
M(t) = a_f - a(t) \leq 0 \tag{5}
\]

\( a_f \) is the final crack size, e.g. crack size at failure. \( a(t) \) is the actual crack size. It is commonly assumed, and recommended in rules of classification societies, that the long-term stress range is Weibull distributed. Hence, the stress effect is defined as the \( m \)-th statistical moment of the distribution, IACS (1999):

\[
\Delta \sigma^m = E[\Delta \sigma^m] = \int_0^\infty f_s(\Delta \sigma) \cdot \Delta \sigma^m \, d(\Delta \sigma) = A^m \cdot \Gamma \left( \frac{m}{B} + 1 \right) \tag{6}
\]

\( A \) and \( B \) are the scale and shape parameter of the distribution, respectively. \( m \) is the material parameter (slope). From the definition of Eq. (1) and (2) the safety margin for failure of a structural detail defined in Eq. (5) may be expressed as:

\[
M(t) = \int_{a_s}^{a_f} \frac{1}{(\varepsilon_{M_k} \cdot M_k(a) \cdot \sqrt{\pi a})^m} da - Cv_0 t A^m \Gamma \left( 1 + \frac{m}{B} \right) \leq 0 \tag{7}
\]

The uncertainty coefficient \( \varepsilon_{M_k} \) affects the magnification factor \( M_k \).

2.3 Updating of model after inspections

According to international conventions and class rules, special surveys of ships are basically carried out every fifth year; the conventional lifetime is assumed 20 years or 25 years in recently issued Common Structural Rules of IACS. IMO (2003) recently issued recommendations about accessibility of tanks. In general visual or close visual inspections are carried out onboard ships, NDT are generally carried out after crack detection for sizing purposes. Leak tests are also required to detect through-thickness cracks. Each inspection provides additional information that can be used to update the reliability model. Each inspection provides an estimate of the size of the cracks in the structure, with all cracks being smaller than the detectable one in ideal cases. Mathematically, the reliability of an inspection is defined by the probability of detection (POD) of fatigue cracks, generally assumed dependent on the actual crack size. However, the POD distribution function could be more accurately defined not only accounting for the inspection method but also considering the effects of the environment, vessel type and conditions and, above all, the surveyor’s skills, fatigue, motivation, etc. Anyhow, a simple and widely applied choice for PODs is the exponential distribution, as it is completely defined by the mean detectable crack size as the only distribution parameter, e.g. Moan et al. (2001).

\[
POD(a) = 1 - e^{-a/\lambda_d} \tag{8}
\]

Updating is based on the concept of conditional probability, i.e. the failure of a structural component given the inspection outcome. The margin event \( M(t) \) for evaluating failure probability, as defined in Eq.(7), is then conditioned by the inspection events:

\[
P_{f,Upd} = P[M(t) \leq 0 | IE_j] = \frac{P[M(t) \leq 0 \cap IE_j]}{P[IE_j]} \tag{9}
\]

Here the \( j \)-th inspection event \( IE_j \) may represent no crack detection or crack detection. The former case implies that cracks are smaller than the smallest detectable crack size at the time of inspection. In the latter case, the actual crack size at the time of the inspection \( t_{\text{insp}} \) is larger than the detectable size. A number of inspections may be carried out and these inspections are considered statistically
independent of each other. The inspection event at time $t_{\text{insp}}$, after $N = v_0 t_{\text{insp}}$ stress cycles, is defined as, *Madsen and Sørensen (1990)*:

$$IE_j(t_{\text{insp}}) = \int_{a_i}^{a_p} \frac{1}{(\varepsilon M_k M_k \sqrt{\pi a})^m} \, da - C v_0(t_{\text{insp}}) \varepsilon_0 A_m \Gamma \left(1 + \frac{m}{B} \right)$$  \hspace{1cm} (10)

The expression in Eq.(10) is positive if a crack is not detected, negative if it is detected. Results of the last performed inspection influence the updating, *Moan and Song (2000)*. Outcomes of previous inspections do not improve the updating, if the result is no crack detection.

Three different strategies can be applied after detection, e.g. the crack may be not repaired at all, can be partially repaired or ‘perfectly’ repaired. The decision can be made depending upon the safety criteria and the risk level the ship manager is willing to assume.

3 Numerical Examples on Typical Details

3.1 Selection of input data

The reliability model for fatigue assessment has been applied to three typical structural details, repeating themselves thousands of times in ship structures:

- A butt joint of two adjacent plates,
- A T-butt (fillet) joint of two perpendicular plates,
- A bracket toe (longitudinal attachment).

Statistical properties of input variables, i.e. in practice mean and standard deviations, were defined and then systematically changed to acquire the sensitivity of the model. The reliability model was improved considering the 2-D crack growth and the membrane and bending stresses. The reliability analyses were carried with the computer programs PROBAN and PROFAST, *DnV (2002)*, according to approximations of FORM (First Order Reliability Method). The values of the input variables were selected as described in the following.

The thickness of all plates was fixed to 20 mm and the attachment length ratio (weld throat/thickness) to 0.75. Inspections are scheduled every fifth year, life is set to 20 years. I reviewed formulations proposed in the literature for the magnification factor $M_k$ to select the most simple and effective one. According to *IIW (1996)*, magnification factors $M_k$ may be estimated by parametric formulations calibrated against FEA (finite element analyses) and experimental analyses, depending basically on geometric ratios of dimensions of welded details. Such formulations give quite accurate results, especially for standard details while more refined analyses are required for special cases. I therefore decided to implement such formulations in the analyses. However, a normally distributed uncertainty coefficient $\varepsilon_{ac}$ was included in the analysis, as per Eqs.(7) and (10), to consider the relevant uncertainties. A 1-D through-thickness crack growth model was initially considered by fixing the crack shape aspect ratio $a/c = 0.2$, but affected by a normally distributed uncertainty coefficient $\varepsilon_{ac}$. The membrane vs. bending stress was initially disregarded, fixing $\alpha = 1$. Each joint is considered to be loaded by a Weibull stress range distribution in lifetime with average load frequency corresponding to $\sim 10^8$ cycles in 20 years. The Weibull shape parameter was calculated according to *IACS (1999)* for a ship length $L = 200$ m and normally distributed considering its variation for ship’s length between 100 m and 300 m:

$$B = 1.1 - 0.35 \cdot \frac{L - 100}{300}$$  \hspace{1cm} (11)

$L$ in [m] is the length. This value should suitably represent the long-term loads for seagoing ships.

The Weibull scale parameter, i.e. the load level, was calculated from values of the appropriate S-N
curves of \textit{DoE (1990)}: curve D for butt joint, curve F for T-butt, and curve F2 for bracket toe. Total damage \(D = 1\) is generally assumed in the hypothesis of linear damage accumulation according to the Miner rule, i.e. the load level was selected such that it leads to failure after \(10^8\) cycles. Hence, the long-term stress range, in terms of the Weibull scale parameter \(A\), is determined for each of the three joints by assuming a target fatigue life of 20 years. Thus, if the cumulative damage \(D\) is fixed, the S-N curve provides the stress range to failure in the lifetime, i.e. in \(N = 10^8\) cycles, given the shape parameter \(B\) of the Weibull function.

Estimates of statistical properties of material parameters are provided e.g. by \textit{DNV (2002), BSI (1999), ISSC (2006b)}: values are in good agreement. A fixed values is usually assigned to \(m\) while all uncertainties are introduced by the \(C\) parameter. The coefficient of variation of \(C\) is reported to be 1-2%.

The initial crack size is a parameter of paramount importance but it is often assumed on the basis of experience and judgment, somehow statistically distributed in reliability analyses. The randomness of the initial crack size is attributed to different factors such as type of joint, welding procedure, control procedures, etc. The initial crack size obtained from data for butt-welded plates was found to be exponentially distributed with a mean value equal to 0.11 mm and a frequency of 16 cracks per m, \textit{Bokalrud and Karlsen (1981)}. \textit{Moan (1998), Moan et al. (2001)} report a mean value of the initial crack size of 0.19 mm determined from actual inspection results on tubular joints of North Sea jackets. For T-butt joints \textit{Burnside et al. (1984)} report the formulation of \textit{Booth} defining the initial crack as a function of material, loading type and plate thickness:

\[
a_i = \frac{0.0198 \cdot t \cdot a_m}{\alpha} \quad a_m = 2.5 \cdot 10^{-5} \cdot \left(\frac{2068}{\sigma_u}\right)^{1.8}
\]  

(12)

where all quantities are to be taken in SI units. \(t\) is the plate thickness and \(a_m\) a material parameter depending on its ultimate strength \(\sigma_u\). The coefficient \(\alpha\) is taken 0.35 for cruciform joints in membrane tension and 0.19 for cruciform joints in bending. For plate having \(t = 20\) mm in steel currently used in shipbuilding, \(a_i\) is therefore in the range 0.05 - 0.2 mm. Eq.(12) is directly and strictly related to the fatigue notch factor proposed by \textit{Peterson (1974)} and subsequently modified by many other researchers. See \textit{Burnside et al. (1984)} for a complete review of this topic. The approach implicitly assumes that an initial crack always exists in the structure and that its value is larger than the lower applicability limit of the Paris-Erdogan law. This hypothesis simplifies the model neglecting the initial phase of the crack growth, which is governed by micro-mechanics and involves physical and chemical parameters.

The detectable crack size depends on the used inspection technique: visual inspection and close visual inspections are essentially used to detect cracks in ships structures. Dye penetrant, magnetic particles, ultrasonic testing or other NDT methods are generally applied for sizing the defect. A mean value of about \(a_D = 1 - 2\) mm corresponds to the use of NDT, \textit{Moan et al. (2006)}, which also implies a mean crack length \(2c = 10 - 20\) mm, if depth/half-length ratio \(a/c = 0.2\). Such value is optimistic for ship structures, as NDT are seldom carried out in place of close visual inspections. Data of detectable crack size for visual inspections available in open literature are mainly qualitative and relevant uncertainties hard to define. The POD is largely affected by ship type, location of cracks, environment, surveyor’s skills, etc., as shown in experiments by \textit{Demsetz and Cabrera (1999)}. Here, I selected an average value of \(a_D = 1.5\) mm for the test cases, taking into account the increasingly stringent requirements of survey procedures imposed by class societies (e.g. Enhanced Survey Programmes, required number of surveyors and their minimum skills for special surveys).

Different definitions are adopted for the critical crack size depending on the limit state considered for the structure: the through thickness crack is the most common in ship structures. Safety coefficients are sometimes included in the calculation assuming half the thickness as critical crack size. However, also the crack size corresponding to the brittle fracture behavior (i.e. to the critical stress intensity factor \(K_{IC}\)), should be considered. The through-thickness criterion is based on serviceability analysis and considers that the structure is not suitable for service under the presence of a through thickness.
crack, due to the possibility of leakage. The brittle-fracture criterion is based on linear fracture mechanics concepts, which state that the unstable fracture occurs in the presence of a given crack dimension that induces a stress intensity factor greater than the material critical stress intensity factor. The serviceability criterion often dominates the definition of final crack size as the critical crack size is higher than plate thickness in normal cases, Berge (2006).

ISSC (2006b) summarized uncertainties included in various probabilistic fatigue analyses available in open literature: their value are in general agreement with the ones described above.

3.2 Results of test cases

Three selected details were analysed, using the basic case input variables summarized in Table I. Figs.1 to 3 give computed probability of failure vs. time. Inspection updating after the fifth, tenth and fifteenth year is also considered assuming as outcome of each inspection either no crack detection or, equivalently, found cracks perfectly repaired.

Figs.4 to 6 show the sensitivity of the Weibull scale parameter $A$ by assuming for its calibration three different damage levels, namely $D = 1.0, 0.5,$ and $0.33$. The failure probability curves shifts downwards. The effect of the load level diminishes after each inspection, because the load is applied for a shorter time interval.

A powerful and useful tool to reduce the 2-D solution of the crack growth law into the 1-D one (i.e. solving only one differential equation) is represented by the parametric equations proposed by Shang-Xian (1985). In the hypothesis of elliptical shape, these solutions reduce the prediction of the shape change to the calculation of elementary functions. Fig.7 compares the results of such model with the ones of the model considering $a/c$ constant, $C_a/C_c = 1.03$. Even quite large uncertainties applied to plate thickness (St. dev. = 0.8mm) and to magnification factor $M_k$ (St. dev. = 0.1) do not have noticeable effects on the 1-D model. The 2-D model gives lower failure probabilities than the 1-D model. Thus the choice of the crack growth model has a large impact on the results. It is debatable which model is the more correct. The influence of the membrane vs. bending stress ratio is rather limited, Fig.8. The behavior is similar to the effects induced by the load level modifications shown in Figs.4 to 6. However, an acting bending stress seems to have a more evident influence after inspection updating.
The sensitivity study also considered the influence of the uncertainty of the date of inspection, since the rules generally allow some months before or after the anniversary date to carry out the survey and implications on inspection planning derive from the uncertainties of the inspection time. Some calculations were carried out adding to the basic models a six months uncertainty, normally distributed, to the inspection time, i.e., the inspection of the 5th, 10th and 15th year can be carried out within six months before or after the anniversary date. The failure probability curves were practically the same for the basic cases shown in Figs.1 to 3 and therefore not reported here. Thus, the common practice of allowing a six months window for special surveys seems reasonable.

<table>
<thead>
<tr>
<th>Variable</th>
<th>Distribution</th>
<th>Mean</th>
<th>Std. dev.</th>
</tr>
</thead>
<tbody>
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<td>Scale param. ln A (butt)</td>
<td>Normal</td>
<td>2.913</td>
<td>0.198</td>
</tr>
<tr>
<td>Scale param. ln A (T-butt)</td>
<td>Normal</td>
<td>2.634</td>
<td>0.198</td>
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<tr>
<td>Scale param. ln A (bracket)</td>
<td>Normal</td>
<td>2.522</td>
<td>0.198</td>
</tr>
<tr>
<td>Stress shape param. B</td>
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<td>0.11</td>
</tr>
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<td>Average load freq. (v_0) [1/s]</td>
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<tr>
<td>Cycles in lifetime (N_0)</td>
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<tr>
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<tr>
<td>Material parameter (m)</td>
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<tr>
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<tr>
<td>Total lifetime [years]</td>
<td>Fixed</td>
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Fig. 7: 2-D crack growth models (T-butt, fillet joint)

Fig. 8: Influence membrane vs. bending stress ratio (plate butt joint)

Fig. 9: Importance factor after 5th year inspection updating

Fig. 10: Importance factors after 10th year inspection updating
4 Sensitivity Analysis

Basic input parameters and their uncertainties were systematically varied to assess their relative importance. A more refined sensitivity analysis may be used as a tool for measuring parameter influence in the safety formulation, or in reliability-based design optimisation.

Importance factors were defined as first-order measures of the weight of a single random variable in the safety problem and they are calculated in FORM analyses as the partial derivative of the reliability index with respect to the mean of the corresponding variable, e.g., Hohenbichler and Rackwitz (1986), Ditlevsen and Madsen (1996). Table II shows importance factors of the main parameters of the test cases before inspections. Figs.9 and 10 show modifications after inspection updating. They are approximately constant during the ship’s life, only slightly modified after inspection updating. Even changing mean value or standard deviation of some input parameters, the relative importance does not change, except when \( M_k \) is defined with a 0.1 standard deviation. In this case the \( M_k \) importance factor became 8% - 10% and the \( \ln A \) and \( \ln C \) ones reduce to approximately 38% and 27%, respectively.

<table>
<thead>
<tr>
<th>Joint</th>
<th>( \ln C )</th>
<th>( a_i )</th>
<th>( \ln A )</th>
<th>( B )</th>
<th>( M_k )</th>
<th>( a/c )</th>
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<td>43</td>
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</tr>
</tbody>
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The inspection quality (expressed by the POD) initially assumes a large importance, but it seems to decrease in few years for the 5th year inspection, Fig.9. Similar behaviour can be noted after 10th year inspections, but less evident, Fig.10. This means that the importance of inspection quality increases with age.

The addition of a limited uncertainty on the material parameter \( m \), i.e. the S-N curve slope, does not really modify the reliability model, as the same failure probabilities of basic case are practically obtained. However, importance factor of \( m \) in this case is about 3%-4%, before inspections and then it decreases. The geometrical parameters such as the plate thickness or the critical crack size are of minor importance in the reliability model, even attributing them a large but reasonable degree of uncertainty.

5 Conclusions

Some general comments can be drawn out from the analyses carried out:

- Selection of input data is challenging and requires efforts and resources, even for simplified reliability models as the one presented here.

- The main parameters of the fatigue reliability model for all three details are the load parameters (Weibull distribution definition) and the material parameter (\( C \) and \( m \) in the crack growth law). The model is more affected by loads uncertainties than by fatigue strength uncertainties.

- After updating, the inspection quality has a dominant importance (about 80% for the bracket toe detail) but rapidly decreases in time while load parameters correspondently increase their importance.

- No significant change in the failure probability is noted when the time of the inspection event is uncertain (e.g. the inspection may occur within a time window of six months as required by current regulations).

- Only the last inspection results significantly contribute in the updating of the model, the effect of the previous inspections to updating is negligible. The optimization of the inspection interval

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(based on no inspection carried out until a target reliability level is reached) could be a possibility to be examined even if for the ships the flexibility required in modifying the inspection time could be worse than inspections at fixed time intervals.

- Pure membrane stresses lead to lower reliability as expected, when also bending stress is acting the updating after inspection seems more efficient.

- 2-D models results should be carefully compared to results from other models. In general, fracture mechanics equations are to be implemented in reliability models, only if adequate input data are available. Otherwise simpler models are easier to apply and to calibrate with experimental data.

The quality of inspection appears to be a key-point together with the loads definition and even if conclusions agree with common sense and engineering judgment. However, a few outcomes appear to be useful to make more efficient surveys of ship structures, showing which input needs to be less uncertain.

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