Time-Dependent Risk Assessment of Aging Ships Accounting for General / Pit Corrosion, Fatigue Cracking and Local Denting Damage

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ABSTRACT

The present paper is a summary of recent research and developments in the areas related to time-dependent risk assessment of aging ship structures, jointly undertaken by the Pusan National University and the American Bureau of Shipping. The age-related structural degradation (i.e., corrosion and fatigue crack) and mechanical damage (i.e., local dent) are considered as types of damage.

The mathematical models for predicting corrosion and fatigue cracking damage are developed as a function of ship age. Two sets of time-dependent corrosion wastage models for 34 longitudinal member groups of tankers / FPSOs, and for 23 longitudinal member groups of bulk carriers are developed by statistical analysis of corrosion measurement data. While initial crack characteristics can be analyzed by the fatigue analysis using the S-N curve approach or detected by survey, crack growth behavior can be assessed by the fracture mechanics approach. The crack size prediction model as a function of ship age is presented in the present paper.

Extensive theoretical, numerical and experimental studies are in this work carried out to investigate the ultimate strength reduction characteristics of ship panels that are wasted due to general or pit corrosion, or have fatigue cracking and local denting damage. The prime concern is with derivation of relevant design formulations for the ultimate strength of structural members with pitting, cracking or local denting damage.

Time-dependent variations of ultimate longitudinal strength reliability/risk of a bulk carrier, a double hull tanker and a ship-type FPSO under vertical bending moments are studied accounting for the effects of general / pit corrosion, fatigue cracking and local denting damage, in both deterministic and probabilistic form. It is aimed at developing more rational repair and maintenance scheme that explicitly takes into account the ultimate longitudinal strength. It is concluded that the insights and methodologies developed from the present study will be very useful for assessing time-dependent risk of aging ships and also establishing damage tolerant design procedures for new ships.

INTRODUCTION

Failures in ship structures, including total losses, continue to occur worldwide, in spite of ongoing continuous efforts to prevent them. Such failures can have enormous costs associated with them, including lost lives in some cases. One of the possible causes of marine casualties is the inability of aging ships to withstand rough seas and weather, because the ship’s structural safety becomes reduced during later life although it is quite adequate at the design stage and perhaps some 15 years beyond.

Ship structures suffer various types of damage while in service. Some types of damage such as corrosion and fatigue cracks are related to ship age, while others such as local dent, collision or grounding damage are mechanical damage caused by accidental loading or impact. Structural damage reduces the load-carrying capacity of the structure, and may cause leakages, resulting in pollutions, cargo mixing or gas accumulating in enclosed spaces. In severe cases, such structural damage may conceivably lead to catastrophic failure or total loss of ships.

In the design and operation of ship structures, there are a number of uncertainties that must be dealt with. Wherever there are uncertainties, a risk of failure exists. For a structure, the risk of failure will for our purposes be defined as the probability that the load-carrying capacity is smaller than the extreme or accidental load that the structure is subjected to.

To minimizing and/or preventing loss of life and financial exposure caused by ship structural failure, it is of vital importance to keep the safety and reliability at an acceptable level even later in life.
As measures of the structural integrity, three terminologies are typically used, namely hazard, reliability and risk. A hazard is meant to be a physical situation with a potential for human injury, damage to property / environment or both. The systematic identification of the hazards with a given system, including potential causes, effects and possible corrective preventive measures is called the hazard identification. Reliability is a measure of the probability of a system or component which ensures to perform the intended function for a specified period of time and under specified operating conditions. The analysis of reliability for a system or component under applied loads in normal or extreme conditions is called the reliability assessment. Risk is a probability associated with both the frequency (event/year) and potential consequences (effects/event) of an accident or a combination of accidents. The analysis of risk which predicts the behavior of a system in terms of accidents, frequencies and consequences in a systematic manner is called the risk assessment.

The present paper is a summary of recent research and developments, jointly undertaken by the Pusan National University and the American Bureau of Shipping. The following seven subjects were studied theoretically, numerically and experimentally (Paik 2002):

- Time-dependent corrosion wastage models for the structures of bulk carriers, oil tankers and ship-type FPSOs,
- Time-dependent fatigue cracking damage models,
- Ultimate strength of structural members with general/pit corrosion,
- Ultimate strength of structural members with fatigue cracking damage,
- Ultimate strength of structural members with local denting damage,
- Time-dependent risk assessment of ship structures taking account of corrosion, fatigue cracking and local denting damage, and
- Repair/maintenance scheme for aging ships.

**PROCEDURE FOR RISK ASSESSMENT**

The procedure for risk assessment of ship structures is similar to that of other types of steel structures. The associated risk may be written, for our purposes, as follows:

\[
\text{Risk} = P_f = \text{Prob}(C \leq D) \quad (1)
\]

where \( P_f \) = probability of failure, \( C = \) load-carrying capacity, \( D = \) demand.

The safety and reliability of a structure is the converse of the risk, i.e., the probability that it will not fail, namely

\[
\text{Reliability} = \text{Prob}(C > D) = 1 - P_f \quad (2)
\]

The result of a standard reliability calculation, normally carried out after transformation of design variables into a standardized normal space, is a reliability index \( \beta \) which is related to the probability of failure \( P_f \) by

\[
P_f = \phi(-\beta) \quad (3)
\]

where \( \phi \) is the standard normal distribution function.

Broadly speaking, risk assessment involves consideration of not only the probability of failure, but also the consequences of failure, ideally in quantitative terms, see for example, Ditlevsen & Madsen (1996) and Ayyub (2001). In our study, the reliability assessment is termed as the converse of the risk assessment, however. This practice has been common in the field of structural reliability for some time now.

A major aim of structural risk assessment for merchant cargo vessels is normally to determine the level of risk in terms of probability as related to total loss from structural causes, and increasingly, the possibility of environmental pollution. An reliability assessment in a "design by analysis" approach usually includes the following tasks:

(a) Specify the required target value of the reliability index (or a target level of risk),
(b) Identify all unfavorable failure modes of the structure or fatal events,
(c) Formulate the limit state function for each failure mode identified in the item (b) above,
(d) Identify the probabilistic characteristics (mean, variance, distribution) of the random variables in the limit state function,
(e) Calculate the reliability against the limit state with respect to each failure mode of the structure,
(f) Assess if the predicted reliability is greater than or equal to the target reliability, and
(g) Repeat the above steps otherwise, as required.

**TARGET RELIABILITY OF SHIPS AGAINST HULL GIRDER COLLAPSE**

The required level of structural safety and reliability may vary from one industry to another depending on various factors such as the type of failure, the seriousness of its consequence or perhaps even the cost of adverse publicity and other intangible losses. Appropriate values of target safety and reliability are not readily available, but may be determined by examining statistics of failures. In doing so, the fundamental difference between a risk assessment and a reliability analysis needs to be acknowledged when interpreting such results.

The methods to select the target level of safeties and reliabilities may be categorized into the following three groups:
“Guessimation”: A “reasonable” value as recommended by a regulatory body or professionals on the basis of prior experience. This method may be employed for innovations for which a statistical database on past failures does not exist.

• Analysis of existing design rules: The level of risk that one has traditionally lived with is estimated by calculating the reliability that is implicitly embedded in existing design rules which have been successful in the past. This method is often used for revisions of existing design rules such as reformating a traditional experience based standard to a reliability based standard.

• Economic value analysis: The target level of safety and reliability is selected to minimize total expected costs during the service life of the structure. This is perhaps the most attractive approach, although it is often difficult to undertake in practice.

Figure 1 shows the reliability indices of some types of ships, which have been previously obtained by different investigators and calculation methods, as a function of the year of the publication (Paik & Frieze 2001). The categories of vessels, governing failure modes and service life are identified. For ships with 20 years of service life, the effect of corrosion damage was accounted for in calculating the β values. It is seen from Figure 1 that the calculated reliability index decreases as the calculation is made in more recent years. This trend does not mean that vessels themselves are becoming less reliable as well, at least not to the extent implied by the trends shown. It certainly is of course true that ship structures have become more efficient over time. But more importantly, some of the calculation results shown are perhaps more notional than the others, and the failure modes considered have become more sophisticated as well. Also, most calculations tend to use the design wave environment as the notional basis, although the actual experience may be relatively more benign, but hard to pin down particularly in trading vessels.

For instance, the calculations in the year of 1974 were made by Mansour (1974). The early results of Mansour demonstrate high notional β values. This appears to be mainly a consequence of obtaining the probability of failure under any wave load cycle rather than under an extreme load. Over time, Mansour has vastly refined and further developed the early calculation methodology from some 25 years ago, and has even successfully unified calculation procedures under the two different wave load criteria (Mansour et al. 1997).

Also, many early pioneering calculations typically used “first yield” as the failure criterion. The first-yield criterion ignores loss of plate effectiveness due to any propensity for buckling and so the location of the neutral axis of a ship’s hull girder during the actual ultimate failure process will not be correctly simulated. This can result in somewhat lower levels of stress being determined for the compression region of the hull girder in addition to the basic panel strength being too high in some cases, implying a higher predicted hull girder bending strength and similarly higher reliability when compared to a reliability calculation based on a more refined prediction of ultimate hull girder bending strength.

Whereas the β values determined in 1991 average around 3.5, these calculated in 2000 average 2.5, and all of these ignore age-related degradation effects that will cause a decrease in the β values further in comparative terms. However, there is to date no escaping the fact that even today’s calculations result in reliability indices that are other than notional and comparative, mostly because of the uncertainties in the loads involved. This situation is expected to continue in the foreseeable future. We can, however, improve the value of comparative and notional reliability measures further by appropriately using the results of continuing advances in load prediction and ultimate strength assessment procedures.

Based on the above varied results, and for purposes of use with the more recent (advanced) methodologies for ultimate hull girder strength calculations, it is considered that β = 2.5 may be a speculative but good target reliability index to aim for in respect of ultimate hull girder strength.

LIMIT STATE EQUATIONS

For hull girder ultimate strength reliability assessment, four levels of failure modes may be considered, namely the primary, secondary, tertiary and quaternary failure modes.

The primary failure mode represents a condition which hull girder collapse takes place by involving buckling collapse of compression flange (i.e., deck panel in sagging
The secondary failure mode indicates a limit state when the flange in compression reaches the ultimate strength. The tertiary failure mode is typically considered when support members in the compression flange fail by lateraltorsional buckling or tripping. The quaternary failure mode is shown when plating between support members in the compression flange reaches the ultimate strength.

The last three failure modes are not necessarily meant to be total loss of a ship, while a ship’s function will be totally lost if the primary failure mode occurs. The limit state equation of each failure mode noted above can be expressible, as follows:

**Primary failure mode:**
\[ F_1 = x_{1u}M_{1u} - (x_{sw}k_{sw}M_{sw} + x_wk_wM_w) \leq 0 \] (4a)

**Secondary failure mode:**
\[ F_2 = x_{2u}M_{2u} - (x_{sw}k_{sw}M_{sw} + x_wk_wM_w) \leq 0 \] (4b)

**Tertiary failure mode:**
\[ F_3 = x_{3u}M_{3u} - (x_{sw}k_{sw}M_{sw} + x_wk_wM_w) \leq 0 \] (4c)

**Quaternary failure mode:**
\[ F_4 = x_{4u}M_{4u} - (x_{sw}k_{sw}M_{sw} + x_wk_wM_w) \leq 0 \] (4d)

where \( M_{iu} \) = hull girder capacity for the \( i \)th failure mode, \( M_{sw} \) = wave-induced bending moment, \( k_{sw}, k_w \) = load combination factors related to still-water bending moment and wave-induced bending moment, respectively, \( x_{sw}, x_w \) = factors taking account of modeling uncertainties associated with hull girder capacity, still-water bending moment and wave-induced bending moment, respectively.

Table 1 Samples of mean and COV of variables related to the modeling uncertainties and load combinations

<table>
<thead>
<tr>
<th>Variable</th>
<th>Distribution</th>
<th>Mean</th>
<th>COV</th>
</tr>
</thead>
<tbody>
<tr>
<td>( x_u )</td>
<td>Normal</td>
<td>1.0</td>
<td>0.10</td>
</tr>
<tr>
<td>( x_{sw} )</td>
<td>Normal</td>
<td>1.0</td>
<td>0.05</td>
</tr>
<tr>
<td>( x_w )</td>
<td>Normal</td>
<td>1.0</td>
<td>0.15</td>
</tr>
<tr>
<td>( k_{sw} )</td>
<td>Fixed</td>
<td>1.0</td>
<td>-</td>
</tr>
<tr>
<td>( k_w )</td>
<td>Fixed</td>
<td>0.9</td>
<td>-</td>
</tr>
</tbody>
</table>

Table 1 represents samples of mean and COV (coefficient of variation) of variables related to modeling uncertainties and load combinations in Equation (4), which will be adopted for reliability assessment of ships later to be illustrated in the present paper.

**Hull Girder Strength Models**

To predict the ultimate hull girder strength, two types of structural idealizations are relevant, namely plate-stiffener combination model or plate-stiffener separation model, as shown in Figure 2.

(a) A typical stiffened plate structure
(b) Plate-stiffener combination model
(c) Plate-stiffener separation model

Figure 2 Two types of structural idealization for a stiffened panel (Paik & Thayamballi 2003)

The formulations for predicting the hull girder capacity are now presented in conjunction with the four types of failure modes described in the previous section.

**Primary Failure Mode**

The ultimate bending moment of a ship hull with positive sign for hogging and negative sign for sagging can be calculated by (Paik et al. 2002, Paik & Thayamballi 2003)

\[ M_{iu} = \sum C \sigma_i A_i(z_i - g_u) + \sum T \sigma_j A_j(z_j - g_u) \] (5)

where \( \sigma_i = \frac{z_i - g}{D - g} \) \( \sigma_{veq} \) for hogging and \( \sigma_i = \frac{g - z_i}{g} \) \( \sigma_{veq} \) for sagging, \( z_i \) = coordinate of the \( i \)th element measured from the base line to the deck with \( z_i = 0 \) at the base line, \( g = \) neutral axis, which is given as:

\[ g = \frac{\sum_c A_{ci} z_i + \sum T A_{j} z_j}{\sum_c A_{ci} + \sum T A_{j}} \]

(\( \sum_j \) is summation for the part in compression or tension, respectively, \( A_{ei} \) = effective cross sectional area of the \( i \)th element in compression, \( A_j \) = cross sectional area of the \( j \)th element in tension, \( \sigma_{veq} \) = average equivalent yield stresses at upper deck or outer bottom panels, \( D = \) depth of the ship.

In calculating the longitudinal bending stress value defined in Equation (5), the following criteria should be satisfied, namely

\[ \sigma_{veq} = \frac{g - z_i}{g} \]

\[ \sigma_{veq} \]
Time-Dependent Risk Assessment of Aging Ships

\[ \sigma \leq \sigma_{\text{Yeq}} \] for tensioned elements \hspace{1cm} (6a)

\[ |\sigma| \leq \sigma_u \] for compressed elements \hspace{1cm} (6b)

where \( \sigma_{\text{Yeq}} \) = equivalent yield stress, \( \sigma_u \) = ultimate compressive stress of the structural element.

When a continuous stiffened plate structure is idealized as an assembly of the plate-stiffener combination units as shown in Figure 2(b), for instance, the ultimate compressive stress of each unit can be predicted, while the ultimate strength of tensioned member is considered to equal the yield stress, as follows (Paik & Thayamballi 2003)

\[
\sigma_u = \frac{\sigma_{\text{Yeq}}}{\sqrt{0.995 + 0.936\lambda^2 + 0.170\beta_x^2 + 0.188\beta_y^2 - 0.67\lambda^4}}
\]

and \( |\sigma_u| \leq \frac{\sigma_{\text{Yeq}}}{\lambda^2} \) \hspace{1cm} (7)

where \( \beta_x = \frac{b}{t \sqrt{E}} \), \( \lambda = \frac{a}{\pi \sqrt{E}} \), \( a \) = plate length (in the ship’s longitudinal direction) between two adjacent transverse frames, \( b \) = plate breadth (in the ship’s transverse direction) between longitudinals, \( t \) = plate thickness, \( E \) = Young’s modulus, \( \sigma_Y \) = yield stress of plating, \( \sigma_{\text{Yeq}} \) = equivalent yield stress of both plating and stiffener.

On the other hand, when the ship hull structure is modeled as an assembly of plate-stiffener separation models as shown in Figure 2(c), the ultimate hull girder strength can also be calculated from Equation (5), but as a function of the ultimate strengths of plating and longitudinals (support members). In this case, the ultimate compressive strength of plating can be predicted from Equation (11a), while that of support members without attached plating may be calculated as the minimum value of the limit loads obtained for the three failure modes, namely lateral-torsional buckling (tripping), beam-column type collapse and Euler column buckling strength adjusted by plasticity correction. Closed-form formulae for predicting the ultimate strength of support members considering types of stiffener profiles (e.g., flat-bar, angle and T-bar) are relevant in textbooks (Paik & Thayamballi 2003).

**Secondary Failure Mode**

The hull girder strength formula based on the secondary failure mode can be given by

\[ M_{2u} = \sigma_u Z_c \] \hspace{1cm} (8)

where \( \sigma_u \), \( Z_c \) = ultimate compressive stress and elastic section modulus, respectively, at the compression flange of a ship.

**Tertiary Failure Mode**

The hull girder strength formula based on the tertiary failure mode can be given by

\[ M_{3u} = \sigma_u Z_c \] \hspace{1cm} (9)

where \( \sigma_u \) = ultimate compressive stress of stiffeners (support members) at the compression flange, \( Z_c \) = as defined in Equation (8).

The ultimate compressive stress \( \sigma_u \) in the tertiary failure mode is meant to be the minimum value of the three strengths of longitudinals at the compression flange, namely elastic-plastic tripping strength, beam-column type collapse strength and Euler column buckling strength adjusted by plasticity correction. Closed-form formulae for predicting the ultimate strength of support members considering types of stiffener profiles (e.g., flat-bar, angle and T-bar) are relevant in textbooks (Paik & Thayamballi 2003).

**Quaternary Failure Mode**

The hull girder strength formula based on the quaternary failure mode can be given by

\[ M_{4u} = \sigma_u Z_c \] \hspace{1cm} (10)

where \( \sigma_u \) = ultimate compressive stress of plating between stiffeners at the compression flange, \( Z_c \) = as defined in Equation (8).

The ultimate compressive stress of plating between stiffeners can be predicted as follows (Paik et al. 2003a) (For symbols not specified below, Equation (7) is referred to):

For \( \frac{a}{b} \geq 1 \):

\[
\frac{\sigma_u}{\sigma_Y} = \begin{cases} 
0.032\beta_x^4 + 0.002\beta_y^2 + 1.0 & \text{for } \beta_x \leq 1.5 \\
1.274/\beta_x & \text{for } 1.5 < \beta_x \leq 3.0 \\
1.248/\beta_y^2 + 0.283 & \text{for } \beta_x > 3.0
\end{cases}
\] \hspace{1cm} (11a)

For \( \frac{a}{b} < 1 \):

\[
\frac{\sigma_u}{\sigma_Y} = \frac{a}{b} \frac{\sigma_{\text{sys}}}{\sigma_Y} + \frac{0.475}{\beta_y^2} \left( 1 - \frac{a}{b} \right)
\] \hspace{1cm} (11b)

where \( \sigma_Y = \) yield stress, \( \beta_x = \frac{b}{t \sqrt{E}} \), \( \beta_y = \frac{a}{t \sqrt{E}} \), \( \sigma_{\text{sys}} \) in Equation (11b) is taken as \( \sigma_{\text{sys}} = \sigma_u \) defined in Equation (11a), but replacing \( \beta_x \) by \( \beta_y \).
**Effect of General/Pit Corrosion on Plate Ultimate Strength**

Corrosion wastage in ship plates can reduce their ultimate strength. Two types of corrosion damage are usually considered, namely general (or uniform) corrosion and localized corrosion. General corrosion reduces the plate thickness uniformly, while localized corrosion such as pitting appears non-uniformly in selected regions, e.g., the vessel bottom in cargo tanks of crude oil carriers.

The ultimate strength of a steel member with general corrosion can be easily predicted, i.e., by excluding the plate thickness loss due to corrosion.

Based on a series of experimental and numerical studies on steel plated structures (Paik 2002), however, it is realized that the plate ultimate strength reduction characteristics due to general corrosion are quite different from those due to pit corrosion. The so-called equivalent plate thickness reduction approach, which represents a pitted plate with a plate of an “equivalent” thickness, is not sufficient for accurately predicting the plate’s ultimate strength.

![Figure 3](image1.png) A sample of the pitting intensity diagram (Degree of pit intensity = 20%)

![Figure 4(a)](image2.png) Collapse test set-up on the box column type of a plated structure with idealized pits

![Figure 4(b)](image3.png) A schematic view of the test structure

Figure 3 shows a sample of pitted plates. Figures 4 and 5 represent examples of the structural models used for the experiment and numerical computations undertaken in the present study.

![Figure 5(a)](image4.png) Idealization of pit size and location

![Figure 5(b)](image5.png) Finite element modeling of a plate with regular pit corrosion (s: symmetric boundary condition)
To assess scale for breakdown due to pit corrosion, a parameter denoted by DOP (degree of pit corrosion intensity) is often used. DOP is defined as the ratio percentage of the corroded surface area to the original plate surface area:

$$\text{DOP} = \alpha = \frac{1}{ab} \sum_{i=1}^{n} A_{pi} \times 100\% \quad (12)$$

where \(n\) = number of pits, \(A_{pi}\) = surface area of the \(i\)th pit, \(a\) = plate length, \(b\) = plate breadth. For a circular type of pit corrosion, \(A_{pi} = \pi d_{ri}^2 / 4\) with \(d_{ri}\) = diameter of the \(i\)th pit.

A series of experimental and numerical studies for steel plated structures with pits and under axial compressive loads or edge shear were performed by varying the DOP, the depth of pit, the regularity of pit, the plate thickness and the plate aspect ratio (Paik 2002).

It is found from the experimental and numerical studies that the ultimate strength of a plate with pit corrosion can be estimated using a strength knock-down factor that can be calculated using the following formulations:

For axial compressive loading:

$$R_{xr} = \frac{\sigma_{xu}}{\sigma_{xuo}} = \left( \frac{A_o - A_r}{A_o} \right)^{0.73} \quad (13)$$

where \(R_{xr}\) = a factor of ultimate compressive strength reduction due to pit corrosion, \(\sigma_{xu}\) = ultimate compressive strength for a member with pit corrosion, \(\sigma_{xuo}\) = ultimate compressive strength for an intact (uncorroded) member, which can be given by Equation (11), \(A_o\) = original cross sectional area of the intact member, \(A_r\) = cross sectional area involved by pit corrosion at the smallest cross section, see Figure 6.

For edge shear:

$$R_{\tau} = \frac{\tau_u}{\tau_{u0}} = \begin{cases} 1.0 & \text{for } \alpha \leq 1.0 \\ -0.18 \ln \alpha + 1.0 & \text{for } \alpha > 1.0 \end{cases} \quad (14)$$

where \(R_{\tau}\) = a factor of ultimate shear strength reduction due to pit corrosion, \(\tau_u\) = ultimate shear strength for a pitted plate, \(\alpha\) = DOP as defined in Equation (12), \(\tau_{u0}\) = ultimate shear strength for an intact plate which can be given by (Paik & Thayamballi 2003)

$$\frac{\tau_{uo}}{\tau_Y} = \begin{cases} 1.324(\tau_{E}/\tau_Y) & \text{for } 0 < \tau_{E} \leq 0.5 \\ 0.039(\tau_{E}/\tau_Y)^3 - 0.274(\tau_{E}/\tau_Y)^2 + 0.676(\tau_{E}/\tau_Y) + 0.388 & \text{for } 0.5 < \tau_{E}/\tau_Y \leq 2.0 \\ 0.956 & \text{for } \tau_{E}/\tau_Y > 2.0 \end{cases} \quad (15)$$

with \(\tau_{E} = k_{r} \pi^2 E (t/b)^2 / 12(1 - \nu^2), \quad k_{r} = 4 \left( \frac{b}{a} \right)^2 + 5.34 \) for \(a/b \geq 1\) or \(k_{r} = 5.34 \left( \frac{b^2}{a^2} \right) + 4.0 \) for \(a/b < 1\), \(\tau_Y = \sigma_Y / \sqrt{3}, \quad \sigma_Y = \text{yield stress of material.}\)

Figure 7 compares Equation (13) with the numerical and experimental results. Figure 8 compares Equation (14) with the non-linear finite element computations. It is evident that the strength knock-down factor approach proposed in the present study is useful for predicting the ultimate compressive or shear strength of pitted plates.
Figure 7 The ultimate compressive strength reduction factor as a function of the smallest cross sectional area for a plate with pit corrosion

\[
\frac{(A_0-A)}{A_0} = (\frac{A_0-A_0}{A_0})^\gamma
\]

Experiment

\[
R_u = (1 - \frac{A_0-A}{A_0})^\gamma
\]

Figure 8 The ultimate strength versus the DOP ratio for a steel plate with pit corrosion under edge shear (Symbols: non-linear finite element calculations)

**Effect of Fatigue Cracking Damage on Plate Ultimate Strength**

Under the action of repeated loading, fatigue cracks may be initiated in the stress concentration areas of the structure. Initial defects or cracks may also be formed in the structure by inappropriate fabrication procedure and may conceivably remain undetected over time. In addition to propagation under repeated cyclic loading, cracks may also grow in an unstable way under monotonically increasing extreme loads, a circumstance which eventually can lead to catastrophic failure of the structure. This possibility is usually tempered by the ductility of the material, and also by the presence of reduced stress intensity regions in a complex structure that may serve as crack arresters even in an otherwise monolithic structure.

For residual strength assessment of aging steel structures under extreme loads as well as under fluctuating loads, it is thus often necessary to take into account a known or premised crack as a parameter of influence.

Figure 9 A schematic of a stiffened steel panel component with three types of crack orientations and under axial compression or edge shear

Figure 10 Various crack locations in the test structure considered in the present study

Figure 11 A sample finite element mesh in a plate with one edge crack and under axial compression
Figure 9 shows a schematic of a stiffened steel panel component with three types of crack orientations and under axial compression or edge shear. Strictly speaking, the ultimate strength behavior of panels depends on the types of crack orientations, among other factors. While pending further studies, the present study considers the cross sectional area as a primary parameter which governs the ultimate strength reduction characteristics, regardless of the crack orientations.

A series of theoretical, numerical and experimental studies for steel plated structures with premised cracks and under axial tensile/compressive loads or edge shear were carried out by varying the location and size of cracks and the plate thickness. Figure 10 shows various crack locations in a plate considered in the present study. Figure 11 shows a sample finite element modelling for a plate with edge crack at one side and under axial compressive loads.

Based on the results of the experiment and non-linear finite element computations obtained from the present study, the strength knock-down factor approach is again suggested for predicting the ultimate strength of a plate with premised cracks, as follows:

For axial tensile / compressive loading:

$$ R_{xc} = \frac{\sigma_{xu} - \sigma_c}{\sigma_{xuo}} $$

(16)

where $R_{xc}$ = a factor of the ultimate tensile or compressive strength reduction due to cracking damage, $\sigma_{xu}$ = ultimate axial strength of cracked plating, $\sigma_{xuo}$ = ultimate axial strength of uncracked plating which may be taken as $\sigma_{xuo} = \sigma_Y$ for axial tensile loading and $\sigma_{xuo} = \sigma_u$ with $\sigma_u$ as defined in Equation (11), $A_o$ = cross sectional area of uncracked (original) plating, $A_c$ = cross sectional area involved by cracking damage.

For edge shear:

$$ R_{tc} = \frac{\tau_u - \tau_{uo}}{\tau_{uo}} $$

(17)

where $R_{tc}$ = a factor the ultimate shear strength reduction due to cracking damage, $\tau_u$ = ultimate shear strength for a plate with premised cracks, $\tau_{uo}$ = ultimate shear strength for an intact plate, as defined in Equation (15).

Figure 12 compares Equation (16) with the experimental and numerical results. Figure 13 compares Equation (17) with the numerical computations. It is again apparent that the proposed formulae reasonably predict the plate ultimate strength at somewhat pessimistic side.
Effect of Local Denting Damage on Plate Ultimate Strength

Plate panels in ships and offshore platforms can have mechanical damage. The inner bottom plates of cargo holds of bulk carriers may be damaged during loading of iron ore when iron ore cargo strikes the plates. In unloading of bulk cargoes such as iron ore or coal, excavators can result in impacts to the inner bottom plates mechanically. Deck plates of offshore platforms may be subjected to impacts due to dropped objects from cranes. Mechanical damages can be denting, cracking, residual stresses or strains due to plastic deformation, and coating damage.

Two types of denting were considered, as shown in Figure 14. Figure 15 defines the geometrical dimensions of the local dent. Figure 16 shows examples of the finite element modeling for a plate with local dent at the plate center. It is concluded that the influence of spherical dent on the plate ultimate strength is similar to that of conical dent, while the former is slightly worse than the latter in terms of load-carrying capacity. Therefore, the spherical dent may be taken as a representative of local dent shape for the purpose of the plate ultimate strength prediction, regardless of the actual shape of denting (Paik et al. 2003b).

Based on the results and insights developed from the present study, empirical formulae for predicting the ultimate compressive strength of dented plates are derived, when \( \frac{d_d}{b} < 1 \), as follows:

For axial compression:

\[
R_{xd} = \frac{\sigma_{ud}}{\sigma_{u0}} = C_3 \left[ C_1 \ln \left( \frac{D_d}{t} \right) + C_2 \right]
\]

where \( R_{xd} \) = a factor of the ultimate compressive strength reduction due to local denting, \( \sigma_{ud} / \sigma_{u0} \) = ultimate compressive strengths of dented or intact plates, respectively. The coefficients \( C_1, C_2 \) and \( C_3 \) are empirically determined by regression analysis of the computed results as follows:

\[
C_1 = -0.042 \left( \frac{d_d}{b} \right)^2 - 0.105 \left( \frac{d_d}{b} \right) + 0.015 \quad (19a)
\]

\[
C_2 = -0.138 \left( \frac{d_d}{b} \right)^2 - 0.302 \left( \frac{d_d}{b} \right) + 1.042 \quad (19b)
\]

\[
C_3 = -1.44 \left( \frac{H}{b} \right)^2 + 1.74 \left( \frac{H}{b} \right) + 0.49 \quad (19c)
\]

where \( H = h \) for \( h \leq 0.5b \) and \( H = b - h \) for \( h > 0.5b \). For axial tension, it is considered \( R_{xd} = 1.0 \).
For edge shear:

\[
R_{td} = \frac{\tau_u}{\tau_{uo}} = \begin{cases} 
C_1 \left( \frac{D_d}{t} \right)^2 - C_2 \left( \frac{D_d}{t} \right) + 1 & \text{for } 1 < \frac{D_d}{t} < 10 \\
100C_1 - 10C_2 + 1 & \text{for } 10 \leq \frac{D_d}{t} \end{cases} \quad (20)
\]

where \( R_{td} \) = a factor of the ultimate shear strength reduction due to local denting, \( \tau_u \) = ultimate compressive strengths of dented plates, \( \tau_{uo} \) = as defined in Equation (15). The coefficients \( C_1 \) and \( C_2 \) are empirically determined by regression analysis of the computed results as follows:

\[
C_1 = 0.0129 \left( \frac{d_d}{b} \right)^{0.26} - 0.0076 \quad (21a)
\]

\[
C_2 = 0.1888 \left( \frac{d_d}{b} \right)^{0.49} - 0.07 \quad (21b)
\]

Figures 17 and 18 compare Equations (18) and (20) with non-linear finite element computations for dented plates under axial compressive loads and edge shear, respectively.

**Effect of Combined Corrosion, Fatigue Cracking and Local Denting Damage**

When pit corrosion, fatigue cracking and local denting damage exist simultaneously, the ultimate strength of a member under axial loads or edge shear may simply be calculated by virtue of a multiplicative model, as follows:

\[
\sigma_{xu} = R_{xv} R_{xc} R_{xd} \sigma_{xuo} \quad (22)
\]

\[
\tau_u = R_{\tau v} R_{\tau c} R_{\tau d} \tau_{uo} \quad (23)
\]

where \( R_{xv}, R_{xc}, R_{xd}, R_{\tau v}, R_{\tau c}, R_{\tau d} \) = as defined in Equations (13), (16), (18), (14), (17) and (20), respectively, \( \sigma_{xuo} \) = ultimate axial strength of an intact member, \( \tau_{uo} \) = ultimate shear strength of an intact member.

**TIME-DEPENDENT CORROSION MODELS**

**Corrosion Mechanism**

The corrosion characteristics in a ship structure are influenced by many factors such as type of corrosion protection, type of cargo, temperature, humidity and so on. This means that it should be able to make estimates of corrosion depth for various different structural members grouped by type and location for different types of ships or cargoes.

Figure 19 is a proposed corrosion process model for a coated area in a marine steel structure. The corrosion behavior is in this model categorized into three phases, on account of a) durability of coating, b) transition to visibly obvious corrosion, and c) progress of such corrosion (e.g., Paik & Thayamballi 2003).
The curve showing corrosion progression as indicated by the solid line in Figure 19 is convex, but it may in some cases be a concave (dotted line). The convex curve indicates that the corrosion rate (i.e., the curve gradient) increases in the beginning but decreases as the corrosion progress proceeds. This type of corrosion progression may be typical of a static immersion environment in sea-water, since the relatively static corrosion scale at the steel surface can eventually disturb the corrosion progression. On the other hand, the concave curve represents a case where the corrosion rate accelerates as the corrosion progress proceeds. This type of corrosion progression may be likely to happen in changing immersion conditions at sea, particularly in dynamically loaded structures where flexing continually exposes additional fresh surface to the corrosion effects. It is considered critical if the ‘domino effect’ of coating breakdown, leading to accelerated corrosion, which reduces scantlings and can ultimately lead to structural failure, is to be minimized or avoided (Contraros 2003).

The life (or durability) of a coating corresponds to the time when a pre-defined and measurable extent of corrosion starts after either a) the time when a newly built ship enters service, b) the application of coating in a previously bare case, or c) repair of a failed coating area in an existing structure to a good intact standard. The life of coating typically depends on the type of coating systems used, details of its application (e.g., surface preparation, stripe coats, film thickness, humidity and salt control during application, etc.), and relevant maintenance, among other factors. While the coating life to a pre-defined state of breakdown is a random variable, it is often treated as a constant parameter.

After the effectiveness of coating is lost, some transition time, i.e., duration between the time of coating effectiveness loss and the time of corrosion initiation, may be considered to exist before the corrosion ‘initiates’ over a large enough and measurable area. The transition time is often considered to be an exponentially distributed random variable. Three types of corrosion models have been suggested as follows:

Paik & Thayamballi (2002):

$$ t_r = C_1(T - T_c - T_t)^{C_2} $$  \hspace{1cm} (24)

where $t_r$ = depth of corrosion (thickness loss due to corrosion), $T_c$ = coating life, $T_t = $ transition time between coating durability and corrosion initiation, $T = $ structure age, $C_1$, $C_2$ = coefficients taking account of the characteristics of corrosion progress.

Guedes Soares & Garbatov (1999):

$$ t_r = t_{ro} \left[1 - \exp \left( - \frac{T - T_c}{T_t} \right) \right] $$  \hspace{1cm} (25)

where $t_{ro} = $ depth of corrosion when the corrosion progress stops, with other symbols defined in Equation (24).

Qin & Cui (2002):

$$ t_r = t_{ro} \left[1 - \exp \left( - \left( \frac{T - T_{st}}{\eta} \right)^{\alpha} \right) \right] $$  \hspace{1cm} (26)

where $T_{st} = $ time when accelerating of corrosion stops, $\alpha$, $\eta = $ coefficients to handle the corrosion decelerating, with other symbols defined in Equations (24), (25).
Figure 21 Sample formulations of the corrosion depth measurements on outer bottom plating of aging bulk carrier structures as a function of ship age, varying the coefficient $C_2$

Figure 21 plots Equation (24) which shows curve fits to a sample of statistical corrosion data for outer bottom shells of bulk carriers, varying the coefficient $C_2$, provided that $T_c = 7.5$ years and $T_t = 0$. The trend of the corrosion progress slightly varies with the coefficient $C_2$ as would be expected, but its effect may be ignored as the ship gets older. For practical design purposes, therefore, it is assumed to be constant regardless of time, i.e., with $C_2 = 1.0$.

Provided that the corrosion initiates without a transition time after the effectiveness of coating is lost, i.e., $T_t = 0$, Equation (24) is simplified to

$$t_r = C_1(T - T_c)$$

(27)

where the coefficient $C_1$ corresponds to the annualized corrosion rate.

Figure 22 represents a schematic of the probability density distribution for the coefficient $C_1$ obtained by statistical analysis of corrosion measurement data for outer bottom plates of bulk carriers. The figure indicates that a Weibull density can be an adequate fit in this case.

The probability density function of the coefficient $C_1$ may then be assumed to follow the Weibull distribution:

$$f_{C_1}(x) = \frac{\lambda}{\alpha} \left( \frac{x}{\alpha} \right)^{\lambda-1} \exp \left[ -\left( \frac{x}{\alpha} \right)^{\lambda} \right]$$

(28)

where $\alpha$ and $\lambda$ are scale and shape parameters, respectively, which will be determined through a probability density fit using the method of moments, the maximum likelihood method or other appropriate method. The choice of a Weibull density function offers some flexibility as it is capable of representing a range of types of exponential behavior.

Figure 23 The 95% and above band or 5% and below band for developing the upper or lower bound corrosion rate models
Mean and COV (coefficient of variation) of the coefficient $C_1$ can then be calculated by statistical analysis once the corrosion measurement data are available. The most probable (or average) level of corrosion damage will be predicted if all gathered corrosion measurement data are used for the statistical analysis. Since the statistical corrosion data are usually very scattered, however, it may also be of interest to investigate the upper or lower corrosion statistical characteristics of the coefficient $C_1$, the former being related to 95% and above band or severe level, while the latter being related to 5% and below band or slight level, as shown in Figure 23.

Corrosion Models for the Structures of Single Hull Tankers and Ship-type FPSOs

Corrosion measurement data for a number of 230 aging single hull tankers carrying crude or product oil has been collected by entities based in Korea and made available to this study. Figure 24 represents the distribution of the ship age that had been surveyed for this purpose.

A total of 33,820 measurements for 34 different member groups (defined by locations, categories and corrosion environments of members) which include 14 categories of plate parts, 11 categories of stiffener webs, and 9 categories of stiffener flanges were obtained and available for the present study.

Table 2 defines the identification of member location/category groups. Figures 25 and 26 represent the most probable (average) or severe level of mean and COV of the coefficient $C_1$ (annualized corrosion rate) for the 34 different member groups, respectively. It is noted that these results were obtained assuming $T_c = 7.5$ years and $T_i = 0$. The corrosion depth of individual member categories can then be predicted from Equation (27) using mean and COV of the coefficient $C_1$ defined in Figures 25 or 26 (Paik et al. 2003c).
Table 2.b Identification of member location/category groups for stiffener web and flange of a single skin tanker structure

<table>
<thead>
<tr>
<th>ID (stiffener web)</th>
<th>Member types</th>
<th>ID (stiffener flange)</th>
<th>Member types</th>
</tr>
</thead>
<tbody>
<tr>
<td>BSLBW</td>
<td>Bottom Shell Longitudinals in Ballast tank – Web</td>
<td>BSLBF</td>
<td>Bottom Shell Longitudinals in Ballast tank – Flange</td>
</tr>
<tr>
<td>SSLBW</td>
<td>Side Shell Longitudinals in Ballast tank – Web</td>
<td>SSLBF</td>
<td>Side Shell Longitudinals in Ballast tank – Flange</td>
</tr>
<tr>
<td>LBLBW</td>
<td>Longitudinal Bulkhead Longitudinals in Ballast tank – Web</td>
<td>LBLBF</td>
<td>Longitudinal Bulkhead Longitudinals in Ballast tank – Flange</td>
</tr>
<tr>
<td>BSLCW</td>
<td>Bottom Shell Longitudinals in Cargo oil tank – Web</td>
<td>BSLCF</td>
<td>Bottom Shell Longitudinals in Cargo oil tank – Flange</td>
</tr>
<tr>
<td>DLCW</td>
<td>Deck Longitudinals in Cargo oil tank – Web</td>
<td>DLCF</td>
<td>Deck Longitudinals in Cargo oil tank – Flange</td>
</tr>
<tr>
<td>SSLCW</td>
<td>Side Shell Longitudinals in Cargo oil tank – Web</td>
<td>SSLCF</td>
<td>Side Shell Longitudinals in Cargo oil tank – Flange</td>
</tr>
<tr>
<td>LBLCW</td>
<td>Longitudinal Bulkhead Longitudinals in Cargo oil tank – Web</td>
<td>LBLCF</td>
<td>Longitudinal Bulkhead Longitudinals in Cargo oil tank – Flange</td>
</tr>
<tr>
<td>BGLCW</td>
<td>Bottom Girder Longitudinals in Cargo oil tank – Web</td>
<td>BGLCF</td>
<td>Bottom Girder Longitudinals in Cargo oil tank – Flange</td>
</tr>
<tr>
<td>DGLCW</td>
<td>Deck Girder Longitudinals in Cargo oil tank – Web</td>
<td>DGLCF</td>
<td>Deck Girder Longitudinals in Cargo oil tank – Flange</td>
</tr>
<tr>
<td>DLBW</td>
<td>Deck Longitudinals in Ballast tank – Web</td>
<td></td>
<td></td>
</tr>
<tr>
<td>SSTLCW</td>
<td>Side Stringer Longitudinals in Cargo oil tank – Web</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Figure 25(a) Mean and COV of the most probable (average) level of the coefficient $C_1$ for plating in a single skin tanker structure
Figure 25(b) Mean and COV of the most probable (average) level of the coefficient $C_1$ for stiffener webs and flanges in a single skin tanker structure

Figure 26(a) Mean and COV of the severe level of the coefficient $C_1$ for plating in a single skin tanker structure

Figure 26(b) Mean and COV of the severe level of the coefficient $C_1$ for stiffener webs and flanges in a single skin tanker structure
Corrosion Models for the Structures of Double Hull Tankers and Ship-type FPSOs

Compared to single hull tankers, very few double hull tankers have reached 15 years old, and only a couple of FPSOs have been in operation more than 10 years (Wang and Spong 2003). Corrosion wastage measurements from double hull tankers and FPSOs are limited and a statistical analysis as mentioned in the previous section does not seem to be available for some years. In view of the many similarities between single hull tankers and double hull tankers, the mean and COV values of the coefficient $C_1$ for the single skin tanker structures as defined in Figures 25 and 26 may be used to those for double skin tankers (Paik et al. 2003c). To supplement limited service performance data, trading tanker experience has provided much of the basis and framework for FPSO design, maintenance and inspection planning.

Figures 27 and 28 represent the most probable (average) or severe level of the annualized corrosion rate for the 34 different member groups of double hull tankers or FPSOs, respectively. Therefore, the corrosion depth of individual member categories can then be predicted from Equation (32) with $T_c = 7.5$ years.
Corrosion Models for Bulk Carrier Structures

The number of ships surveyed was 109 and their size was in the range of 6,095 DWT to 138,655 DWT. Measured data for the corrosion wear for a total of 109 bulk carrier structures carrying iron ore and coal have been collected by entities based in Korea and made available to this study. Figures 29(a) and 29(b) represent the distribution of the age and the deadweight for the 109 bulk carriers that had been surveyed for this purpose.

Table 3 Identification of the 23 member groups for a bulk carrier structure

<table>
<thead>
<tr>
<th>ID</th>
<th>Member type</th>
</tr>
</thead>
<tbody>
<tr>
<td>OBP</td>
<td>Outer bottom plates</td>
</tr>
<tr>
<td>IBP</td>
<td>Inner bottom plates</td>
</tr>
<tr>
<td>LSP</td>
<td>Lower sloping plates</td>
</tr>
<tr>
<td>LWTSS</td>
<td>Lower wing tank side shells</td>
</tr>
<tr>
<td>SS</td>
<td>Side shells</td>
</tr>
<tr>
<td>UWTSS</td>
<td>Upper wing tank side shells</td>
</tr>
<tr>
<td>USP</td>
<td>Upper sloping plates</td>
</tr>
<tr>
<td>UDP</td>
<td>Upper deck plates</td>
</tr>
<tr>
<td>BG</td>
<td>Bilge girders</td>
</tr>
<tr>
<td>OBLW</td>
<td>Outer bottom longitudinals – web</td>
</tr>
<tr>
<td>OBLF</td>
<td>Outer bottom longitudinals – flange</td>
</tr>
<tr>
<td>IBLW</td>
<td>Inner bottom longitudinals – web</td>
</tr>
<tr>
<td>IBLF</td>
<td>Inner bottom longitudinals – flange</td>
</tr>
<tr>
<td>UWTSLW</td>
<td>Upper wing tank side longitudinals – web</td>
</tr>
<tr>
<td>UWTSFL</td>
<td>Upper wing tank side longitudinals – flange</td>
</tr>
<tr>
<td>USTLW</td>
<td>Upper sloping longitudinals – web</td>
</tr>
<tr>
<td>USTLF</td>
<td>Upper sloping longitudinals – flange</td>
</tr>
<tr>
<td>UDLW</td>
<td>Upper deck longitudinals – web</td>
</tr>
<tr>
<td>UDLF</td>
<td>Upper deck longitudinals – flange</td>
</tr>
<tr>
<td>LWTSLW</td>
<td>Lower wing tank side longitudinals – web</td>
</tr>
<tr>
<td>LWTSLF</td>
<td>Lower wing tank side longitudinals – flange</td>
</tr>
<tr>
<td>LSLW</td>
<td>Lower sloping longitudinals – web</td>
</tr>
<tr>
<td>LSLF</td>
<td>Lower sloping longitudinals – flange</td>
</tr>
</tbody>
</table>

A total of 12,446 measurements for 23 longitudinal members (defined by locations and categories of members) which include 9 categories of plate parts, 7 categories of stiffener webs, and 7 categories of stiffener flanges were obtained and available for this study, as indicated in Table 3.

Figures 30 and 31 show mean and COV of the average or severe level of the coefficient $C_i$ for the 23 member location/category groups of a bulk carrier structure, respectively. As previously described, the average corrosion rate is based on all gathered corrosion data, while the severe corrosion rate is based on 95% and above band of the corrosion measurements, see Figure 23. Therefore, the corrosion depth of individual member categories can then be predicted from Equation (27) with $T_c=7.5$ years, except for inner bottom plates(IBP) and lower slope plates(LSP) which may take a shorter coating life, i.e., $T_c=5$ years.
TIME-DEPENDENT FATIGUE CRACKING DAMAGE MODELS

Fatigue Cracking Mechanism

Fatigue cracking damage has been a primary source of costly repair work of aging ships. Cracking damage has been found in welded joints and local areas of stress concentrations, e.g., at the weld intersections of longitudinals, frames and girders. Initial defects may also be formed in the structure by fabrication procedure and may conceivably remain undetected over time. Under a cyclic loading or even monotonic extreme loading, cracking may propagate and become larger with time.

Since cracks can conceivably lead to catastrophic failure of the structure, it is essential to properly consider and establish relevant crack tolerant design procedures for ship structures, in addition to implementation of close-up survey strategy. For reliability assessment of aging ship structures under extreme loads, it is often necessary to take into account a known (existing or premised or anticipated) crack on the ultimate limit state analysis as a parameter of influence. To make this possible, it is required to develop time-dependent fatigue cracking model which can predict the cracking damage in location and size as the ship gets older.

Figure 32 shows a schematic of fatigue cracking progress as a function of time (age) in steel structures. The fatigue cracking progress can be separated into three stages: initiation (stage I), propagation (stage II) and failure (fracture) stage (stage III) (ISO 2394 1998).

It may be assumed that no initial defects exist so that there is no cracking damage until time $T_1$. While the fatigue cracking damage is affected by many factors such as the stress ranges experienced during the load cycles, local stress concentration characteristics, and the number of stress range cycles, the initiation of cracking can be evaluated by the fatigue analysis.
On the other hand, when any crack is detected in an existing structure at time \( T_0 \), it has normally a certain amount of crack size (length), denoted by \( a_0 \) called the initial crack size, which must be detectable. For the assessment of time-dependent risks of a structure, however, it is often assumed that the initial crack size is small, say 1.0mm.

Fatigue cracks propagate with time progressively in ductile material. They may however become quite unstable in brittle material. Crack propagation is affected by many parameters such as initial crack size, history of local nominal stresses, load sequence, crack retardation, crack closure, crack growth threshold and stress intensity range in addition to stress intensity factor at the crack tip which depends on material properties and geometry. The fracture mechanics approach is often used to analyze the behavior of crack propagation.

The time-dependent cracking damage model may also be composed of the three separate models:

- A model for crack initiation assessment or detection
- A model for crack growth assessment
- A model for failure assessment

Crack initiation at a critical structural detail can be theoretically predicted using the S-N curve approach (\( S = \) fluctuating stress, \( N = \) associated number of stress cycles). The Palmgren-Miner cumulative damage rule is applied together with the relevant S-N curve. This normally follows three steps: (a) define the histogram of cyclic stress ranges, (b) select the relevant S-N curve, and (c) calculate the cumulative fatigue damage and judge the initiation of crack.

Cracks at critical joints and details can be detected during inspection when the crack size is larger enough, usually about 15-30mm. Since the present study focuses on the integrity of existing aged ship structures, it is assumed that the crack with length of \( a_0 \) at a critical joint or detail has initiated at \( T_0 \) years.

Crack growth can be assessed by the fracture mechanics approach which considers that one or more premised cracks of a small dimension exist in the structure, and predicts the fatigue damage during the process of their crack propagation including any coalescence and break through the thickness, and subsequent fracture. In this approach, a major task is to pre-establish the relevant crack growth equations or ‘laws’ as a function of time (year).

The crack growth rate is expressed as a function of the stress intensity factor at the crack tip, on the assumption that the yielded area around the crack tip is relatively small. The so-called Paris-Erdogan law is often used for this purpose, and being expressed as follows:

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

(29)

where \( \Delta K = \) stress intensity factor at the crack tip, \( C, m = \) constants to be determined based on tests, \( a = \) crack length, \( N = \) number of stress cycles.

For steel structures with typical types of cracks, the stress intensity factor formulae are given in textbooks (Broek 1986, Paik & Thayamballi 2003). In ship stiffened panels, cracks are often observed along the weld intersections between plating and stiffeners. For a plate with cracking, \( \Delta K \) may be given, when stiffening effect is neglected, as follows:

\[
\Delta K = F \Delta \sigma \sqrt\pi a
\]  

(30)

where \( \Delta \sigma = \) stress range (or double amplitude of applied fatigue stress), \( a = \) crack size (length), \( F = \) geometric parameter depending on the loading and configuration of cracked body.

**Fatigue Cracking Damage Model**

In the time-dependent risk assessment considering the growth of fatigue cracking, it is more efficient to express the crack growth behavior in a closed-form.

The crack length, \( a(T) \), as a function of time, \( T \), can then be calculated by integrating Equation (29) with regard to the stress cycle, \( N \). In the integration of Equation (29), it is often assumed that the geometric parameter, \( F \), is constant, i.e., assuming that the geometric parameter, \( F \), is unchanged with the crack propagation. This assumption is reasonable as long as the initial crack size, \( a_0 \), is small.

In this case, the integration of Equation (29) after substituting Equation (30) into Equation (29) results in

\[
a(T) = \begin{cases} \frac{a_0}{m-2} \left( 1 - \frac{m}{2} \right) (\Delta \sigma \sqrt\pi)^m (T - T_0)^{1 - m} & \text{for } m < 2 \\ a_0 \exp \left[ (\Delta \sigma \sqrt\pi)^m (T - T_0) \right] & \text{for } m = 2 \end{cases} \]  

(31)
where \( a_0 \) = initial crack size, \( a \) = total crack size.

It is roughly considered that in ships the expected number of wave load cycles occurs once in every 6-10 seconds, and hence

\[
N \approx \left( T - T_0 \right) \times 365 \times 24 \times 60 \times 60 / 10 = \omega \times \left( T - T_0 \right) \quad \text{or} \quad \omega \approx 365 \times 24 \times 60 \times 60 / 10
\]

where \( T \) = ship age, \( T_0 \) = age at the initiation of the cracking with \( a = a_0 \).

Figure 33 A comparison of Equation (31) with a direct (numerical) integration of Equation (29) for a small initial crack size

Figure 33 shows a sample application of Equation (31) by comparing with a direct integration of Equation (29) which accounts for the effect of crack growth on the geometric parameter, \( F \), i.e., being varied with time. It is seen from Figure 33 that Equation (31) slightly overestimates the fracture life as the crack propagates. This is expected because Equation (31) was derived under the assumption that the geometric parameter, \( F \), remains unchanged with time. However, it is considered that their difference for a small initial crack size is negligible, and Equation (31) gives a reasonable tool for the crack growth assessment.

**TIME-DEPENDENT RISK ASSESSMENT OF SHIPS: ILLUSTRATIVE EXAMPLES**

**The Object Ships**

The time-dependent risk assessment for three vessels, namely a 105k dwt double hull tanker, a Capesize bulk carrier and a 254k dwt single hull tanker-type FPSO, is now carried out with focus on the primary failure mode, i.e., hull girder collapse, taking into account the influence of age related structural degradation (corrosion and fatigue cracking) and local denting.
imperfections (initial deflection and residual stresses).

Generally, the three vessels have enough safety margin as long as structural damage does not exist. Nevertheless, a variety of structural failure modes including local plate buckling, yielding and lateral-torsional buckling of stiffeners or longitudinals take place before the ship hulls reach the ultimate limit state.

Traditionally, the longitudinal strength safety measure is defined as a ratio of the section modulus \( Z \) to the IACS required section modulus \( Z_{\text{min}} \). An alternative to measure the safety of a ship’s longitudinal strength is the ratio of ultimate strength \( uM \) over the maximum total bending moment \( M_t \). Figure 35 compares these two safety measures.

<table>
<thead>
<tr>
<th>Property</th>
<th>Double hull tanker</th>
<th>Bulk carrier</th>
<th>Single hull tanker-type FPSO</th>
</tr>
</thead>
<tbody>
<tr>
<td>LBP (L)</td>
<td>233.0 m</td>
<td>282.0 m</td>
<td>313.0 m</td>
</tr>
<tr>
<td>Breadth (B)</td>
<td>42.0 m</td>
<td>50.0 m</td>
<td>48.2 m</td>
</tr>
<tr>
<td>Depth (D)</td>
<td>21.3 m</td>
<td>26.7 m</td>
<td>25.2 m</td>
</tr>
<tr>
<td>Draft (d)</td>
<td>12.2 m</td>
<td>19.3 m</td>
<td>19.0 m</td>
</tr>
<tr>
<td>Block coeff. (C_b)</td>
<td>0.833</td>
<td>0.826</td>
<td>0.833</td>
</tr>
<tr>
<td>Design speed</td>
<td>16.25 knots</td>
<td>15.15 knots</td>
<td>-</td>
</tr>
<tr>
<td>DWT</td>
<td>105,000 DWT</td>
<td>170,000 DWT</td>
<td>254,000 DWT</td>
</tr>
<tr>
<td>Cross-sectional area</td>
<td>5.318 m^2</td>
<td>5.652 m^2</td>
<td>7.858 m^2</td>
</tr>
<tr>
<td>Height to neutral axis from base line</td>
<td>9.188 m</td>
<td>11.188 m</td>
<td>12.173 m</td>
</tr>
<tr>
<td>I</td>
<td>359.480 m^3</td>
<td>694.307 m^3</td>
<td>863.693 m^3</td>
</tr>
<tr>
<td>Z</td>
<td>1152.515 m^3</td>
<td>1787.590 m^3</td>
<td>2050.443 m^3</td>
</tr>
<tr>
<td>Bottom</td>
<td>39.126 m^3</td>
<td>62.058 m^3</td>
<td>70.950 m^3</td>
</tr>
<tr>
<td>Horizontal</td>
<td>44.354 m^3</td>
<td>66.301 m^3</td>
<td>-</td>
</tr>
<tr>
<td>Deck</td>
<td>11.930 GNm</td>
<td>20.650 GNm</td>
<td>22.615 GNm</td>
</tr>
<tr>
<td>Bottom</td>
<td>19.138 GNm</td>
<td>31.867 GNm</td>
<td>31.202 GNm</td>
</tr>
</tbody>
</table>

Notes: FPSO = floating, production, storage and offloading unit, \( I \) = moment of inertia, \( Z \) = section modulus, \( \sigma_Y \) = yield stress, \( M_p \) = fully plastic bending moment.
It is noted that for wave load prediction purposes, the ship-type FPSO is assumed to have an equivalent operational speed of 10 knots in waves, while it usually remains at a specific location once installed. The significant wave heights of the FPSO considered are representative values obtained from the wave statistics in the corresponding installation sites (e.g., Wang et al. 2002).

Based on the steepest boundaries of global wave scatter diagrams, the 1979 ISSC Environmental Conditions Committee I.1 defines extreme climatic waves of limiting steepness which are close to Hogben’s contemporary work (Hogben et al. 1986, Hogben 1990), giving the zero-up-crossing wave period $T_z$, as follows:

$$T_z = \sqrt{13H_s} \quad (32a)$$

where $H_s$ = significant wave height (m). Therefore, the number $N$ of wave peaks which a vessel encounters during a storm duration can approximately be given by

$$N = T_z / T_s \quad (32b)$$

where $T_s$ = storm duration in seconds. For 3 hours storm duration with $H_s = 12.2$ m, for instance, the number of wave peaks then becomes $N = 3 \times 60 \times 60 / \sqrt{13 \times 12.2} = 857$.

**Scenarios for Structural Damage**

Ship structures typically suffer age related structural degradation such as corrosion and fatigue cracking. Inner bottom plates of bulk carriers may also have local denting damage during loading and unloading of cargo. The age related structural damage needs to be dealt with as a function of ship age, while local dent can be treated as time-independent damage.

**Corrosion Damage**

Annualized corrosion rate models for different structural member groups by type and location, considering plating, and webs and flanges of stiffening were in the present study developed by statistical analysis of corrosion wear measurements. These models can be used to predict the general/pit corrosion depth (diminution) of primary members as the ships get older. Since the structural characteristics and corrosion environments of tankers are similar to those of ship-type FPSOs, the corrosion models developed for tanker structures may be employed for FPSO structures (Paik et al. 2003c, Wang et al. 2003a, 2003b).

In the present risk assessment, two levels of corrosion rates, namely the average and severe level are considered, the former being based on all collected data of corrosion measurements and the latter being based on 95% and above band of the measurements. While it is assumed that
corrosion starts immediately after the breakdown of coating, the coating life of all structural members in the double hull tankers, bulk carriers and FPSOs is considered to be 7.5 years except for inner bottom plates and lower sloping plates in the bulk carrier which have 5 years of the coating life (Paik et al. 2003d).

Figure 36 shows the average level progress of corrosion depth for some selected members as the vessels get older.

There are several types of corrosion possible for mild and low alloy steels in marine applications. The so-called general (or uniform) corrosion refers to uniform wastage of thickness. Localized corrosion such as pitting is more likely to occur in bottom plates.

The ultimate strength behavior of ship structures with general corrosion is somewhat different from that with pitting corrosion (Paik & Thayamballi 2003). More realistic assessment of structural integrity of aging ships should take into account both pit and general corrosion.

For any structural member with general corrosion, it is considered that the member thickness is uniformly reduced following the corrosion rate with time. The ultimate strength of corroded plating is predicted by using the design formulae excluding the plate thickness loss. The ultimate strength of a structural member with pit corrosion is calculated by that of un-corroded member multiplied with the ultimate strength reduction factor derived in the previous sections.

In the present risk assessment, it is assumed that the most heavily pitted cross section of any structural member expands over the plate breadth. This may provide somewhat pessimistic evaluation of residual strength, but should be sufficient for illustrating the risk assessment approach.

Fatigue Cracking

The time-dependent fatigue cracking model has been established in the present study. The crack length of any critical area is then predicted by a closed-form formula as a function of ship age. It is assumed that cracking initiates at all stiffeners and plating after 5 years of the ship age. The initial crack size is considered to be 1.0mm.
\[ \Delta \sigma_i = 2 \times \sigma_{x_i} \times \text{SCF}_i \times k_f = 2 \times \sigma^*_x \times \text{SCF}_i \]  
(33)

where \( \sigma_x \) = cyclic “peak” stress amplitude which may be given by \( M_w \times \frac{z}{I} \), \( \sigma^*_x = k_f \times \sigma_x \), \( M_w \) = wave-induced component of bending moment, \( I \) = time-dependent moment of inertia, \( z \) = distance from the time-dependent neutral axis to the point of stress calculation, \( k_f \) = the knock-down factor accounting for the dynamic stress cycles.

SCF in Equation (33) is the stress concentration factor at any critical joint, which takes into account geometric effects. For the present illustrative example purposes, it is assumed that the SCF at joints of plating and stiffeners (or support members) is 2.1. However, hold frame connections to upper or lower wing tanks and side shell of bulk carrier have SCF=3.75, and hopper knuckles have a much larger SCF, i.e., 10.5. It should be noted that SCF’s for different joints can be determined using more refined calculation approaches, and those for typical joints in ship structures may be obtained from some established tables (ABS 2002). The knock-down factor \( k_f \) accounting for the dynamic stress cycles is taken as 0.25 for the sake of simplicity.

Figure 37 shows time-dependent hull girder bending stress ranges calculated from Equation (33) for some selected critical joints of the three object vessels. Figure 38 shows the resulting crack growth characteristics which are evaluated by Equation (31).
Figure 38(b) Crack growth characteristics of some critical joints in the 170k dwt bulk carrier

Figure 38(c) Crack growth characteristics of some critical joints in the 254k dwt single hull tanker-type FPSO

Table 6 The probabilistic characteristics for random variables used for the present risk assessment

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Definition</th>
<th>Distribution Function</th>
<th>Mean</th>
<th>COV</th>
</tr>
</thead>
<tbody>
<tr>
<td>E</td>
<td>Elastic modulus</td>
<td>Normal</td>
<td>205.8GPa</td>
<td>0.03</td>
</tr>
<tr>
<td>σ_Y</td>
<td>Yield stress</td>
<td>Log-Normal</td>
<td>As for each member</td>
<td>0.10</td>
</tr>
<tr>
<td>t_p</td>
<td>Thickness of plating</td>
<td>Fixed</td>
<td>As for each member</td>
<td>-</td>
</tr>
<tr>
<td>t_w</td>
<td>Thickness of stiffener web</td>
<td>Fixed</td>
<td>As for each member</td>
<td>-</td>
</tr>
<tr>
<td>t_f</td>
<td>Thickness of stiffener flange</td>
<td>Fixed</td>
<td>As for each member</td>
<td>-</td>
</tr>
<tr>
<td>T</td>
<td>Ship age</td>
<td>Fixed</td>
<td>As for each age</td>
<td>-</td>
</tr>
<tr>
<td>T_c</td>
<td>Coating life</td>
<td>Normal</td>
<td>5.0 years</td>
<td>0.40</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>7.5 years</td>
<td>0.40</td>
</tr>
<tr>
<td>C_l</td>
<td>Corrosion rate</td>
<td>Weibull</td>
<td>As for each member</td>
<td>As for each member</td>
</tr>
<tr>
<td>a_o</td>
<td>Initial crack size</td>
<td>Normal</td>
<td>1.0 mm</td>
<td>0.20</td>
</tr>
<tr>
<td>C</td>
<td>( \frac{da}{dN} = C(\Delta K)^m )</td>
<td>Log-Normal</td>
<td>6.94E-12</td>
<td>0.20</td>
</tr>
<tr>
<td>m</td>
<td>( \frac{da}{dN} = C(\Delta K)^m )</td>
<td>Fixed</td>
<td>3.07</td>
<td>-</td>
</tr>
<tr>
<td>d_d</td>
<td>Diameter of local dent</td>
<td>Normal</td>
<td>0.3b</td>
<td>0.10</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>0.5b</td>
<td>0.10</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>0.8b</td>
<td>0.10</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>1.0t_p</td>
<td>0.10</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>2.5t_p</td>
<td>0.10</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>5.0t_p</td>
<td>0.10</td>
</tr>
</tbody>
</table>

Notes: \( a \) = crack size, \( b \) = plate breadth, \( N \) = number of stress cycles, \( \Delta K \) = stress intensity factor
At a given age, the ultimate strength of structural members with known (premised) fatigue cracking damage is then predicted by the simple design formula developed in the present study. It is considered that fracture takes place if the crack size (length) of the member reaches the critical crack size, assumed to be either the plate breadth or the stiffener web height.

Table 7 lists the probabilistic characteristics for random variables used for the risk assessment associated with fatigue cracking.

**Local Dent**

Inner bottom plates of bulk carriers can have local dent damage caused by mishandled loading and unloading of dense cargo such as iron ore.

It is assumed that inner bottom plating of the bulk carrier have local dent damage after 5 years. For the present illustrative purposes, the size of local dent is assumed to be the same at all inner bottom plating, namely \( \frac{D_d}{t} = 2.5 \) and \( \frac{d_d}{b} = 0.5 \), where \( D_d \) = dent depth, \( d_d \) = dent diameter, \( b \) = plate breadth between bottom longitudinals, \( t \) = plate thickness.

The ultimate strength of plating with local dent damage is then predicted by design formulae developed in the present study. The COV associated with the local dent related parameters is assumed as defined in Table 6.

**Still-water Bending Moment Calculations**

\( M_{sw} \) in Equation (4) is taken as the maximum value of the still-water bending moment resulting from the worst load condition for the ship, considering both hogging and sagging. The related detailed distribution of the still-water bending moment along the ship’s length can be calculated by a double integration of the difference between the weight force and the buoyancy force, using the simple beam theory.

For convenience, the mean value of \( M_{sw} \) is often taken from an empirical formula that has been suggested for a first cut estimation of the maximum allowable still-water bending moment by some Classification Societies in the past. That approximate formula is given by

\[
M_{sw} = \begin{cases} 
0.072L & \text{for } L \leq 90 \\
10.75 - \left( \frac{300 - L}{100} \right)^{1.5} & \text{for } 90 < L \leq 300 \\
10.75 & \text{for } 300 < L \leq 350 \\
10.75 \left( \frac{L - 350}{150} \right)^{1.5} & \text{for } 350 < L \leq 500 
\end{cases}
\]

where \( C = \left\{ \begin{array}{ll}
7.0 & \text{for hogging} \\
11.0 & \text{for sagging}
\end{array} \right. \)

with \( L \) = ship length (m), \( B \) = ship breadth (m), \( C_b \) = block coefficient at summer load waterline.

Table 7 indicates the calculated still-water bending moments for the three vessels computed at the operational conditions and sea states assumed. The COV associated with still-water bending moment of a merchant cargo vessel is normally large, perhaps as high as 0.4. The variation in still-water bending moment is usually assumed to follow the normal distribution.

**Wave-induced Bending Moment Calculations**

For reliability assessment of newly built ships, \( M_w \) in Equation (4) is normally taken as the mean value of the extreme wave-induced bending moment which the ship is likely to encounter during its life time, which is given for unrestricted worldwide service by the IACS, as follows

\[
M_w = \begin{cases} 
+0.19C L^2 B C_b & \text{for hogging} \\
-0.11C L^2 B(C_b + 0.7) & \text{for sagging}
\end{cases}
\]

where \( C, L, B, C_b \) = as defined in Equation (34).

For the safety and reliability assessment of damaged ship structures in particular cases, short-term based response analysis may be used to determine \( M_w \) when the ship encounters a storm of specific duration (usually 3 hours) and with certain small encounter probability. The MIT sea-keeping tables developed by Loukakis & Chryssostomidis (1975) are useful for predicting the short-term based wave-induced bending moment of merchant cargo vessels. The MIT sea-keeping tables are designed to efficiently determine the root-mean-square value of the wave-induced bending moment given the values of effective wave height, \( d/B \) ratio, \( B/L \) ratio, ship operating speed, the block coefficient, and sea state persistence time. USAS-L program which automates the MIT sea-keeping tables can be downloaded from the internet web site (Paik & Thayamballi 2003).

The most probable extreme value of the wave-induced loads, \( M_w \), i.e., mode, which we may refer to as a mean for convenience and its standard deviation, \( \sigma_{sw} \), can then be computed based on up-crossing analysis as follows.


\[ M_w = \sqrt{2\lambda_o \ln N} + \frac{0.5772}{\sqrt{2\lambda_o \ln N}} \]  

\[ \sigma_w = \frac{\pi}{\sqrt{6}} \frac{\lambda_o}{2\ln N} \]

where \( \sqrt{\lambda_o} \) is the root-mean-square value of the short-term wave-induced bending moment process at amidships. N is the expected number of wave bending peaks. For example, if a peak normally occurs once in every \( T_z \) seconds obtained from Equation (32a), and hence in a storm of 3 hour persistence time, \( N = \frac{3 \times 60 \times 60}{T_z} \).

Table 7 indicates the wave induced bending moments for the three vessels computed at the operational conditions and sea states assumed. The COV associated with the wave-induced bending moment can be defined as \( \text{COV} = \sigma_w / M_w \) from Equation (36) on which the short-term response is based upon, while it is sometimes assumed to be 0.1 when \( M_w \) is predicted from Equation (36a).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Parameter</th>
<th>Double hull tanker</th>
<th>Bulk carrier</th>
<th>FPSO</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \text{M}_w \ )</td>
<td>Mean</td>
<td>-2.318 GNm</td>
<td>-4.210 GNm</td>
<td>-5.058 GNm</td>
</tr>
<tr>
<td></td>
<td>COV</td>
<td>0.40</td>
<td>0.40</td>
<td>0.40</td>
</tr>
<tr>
<td>( \text{M}_w \ )</td>
<td>Mean</td>
<td>2.559 GNm</td>
<td>4.673 GNm</td>
<td>5.584 GNm</td>
</tr>
<tr>
<td></td>
<td>COV</td>
<td>0.40</td>
<td>0.40</td>
<td>0.40</td>
</tr>
</tbody>
</table>

Notes: GOM = Gulf of Mexico, NS = North Sea.

Figure 39 shows variations of the wave-induced bending moments as a function of significant wave height, obtained by using the MIT sea-keeping tables, for the three object vessels in a storm during 3 hour persistence time. The IACS formula values by Equation (35) are also compared. It is seen from Figure 39 that the wave-induced bending moment significantly increases as the significant wave height increases. Also, for the particular scenarios of operational condition and sea states presumed in the present study, it is observed that the short-term based wave-induced bending moments are smaller than the IACS formula by 10 to 20%.

\textbf{Time-Dependent Risk Assessment}

The formula for hull girder ultimate strength is eventually expressed as a function of design parameters related to geometric, structural scantlings and material’s properties.
When time-dependent structural degradation (e.g., corrosion, fatigue cracking damage) and local dent is considered, the value of member thickness at any particular time is a function of such damage. This results in

\[
M_{\text{us}} = M_{\text{us}}[E_i, \sigma_y, t_{\text{us}}, t_{\text{us}}, t_{\text{us}}, T_i, C_i, a_{\text{us}}, m_i, d_{\text{us}}, D_{\text{us}}] \quad (37a)
\]

\[
M_{\text{uh}} = M_{\text{uh}}[E_i, \sigma_y, t_{\text{uh}}, t_{\text{uh}}, t_{\text{uh}}, T_i, C_i, a_{\text{uh}}, m_i, d_{\text{uh}}, D_{\text{uh}}] \quad (37b)
\]

where \(M_{\text{us}}, M_{\text{uh}}\) = ultimate hull girder strength in sagging or hogging. The subscript \(i\) indicates the \(i\)th structural member.

In reliability assessment, all the parameters noted in Equation (37) are treated as random variables, with the probabilistic characteristics (i.e., mean, COV and distribution function) as defined in Table 6. The structural damage scenarios are divided into the following five groups:

- Intact (undamaged)
- Corrosion damage alone
- Corrosion + local dent damage (only for the bulk carrier in hogging)
- Corrosion + fatigue cracking damage
- Corrosion + fatigue cracking and local dent damage (only for the bulk carrier in hogging)

Figures 40 to 42 show the time-dependent characteristics of ultimate hull girder strength, reliability or risk of the three object vessels varying the damage scenarios when no repair or renewal is made. As the vessels get older, the corrosion depth and cracking size (length) grow and thus the ultimate hull girder strength or the reliability index decreases, while the level of the risk against hull girder collapse increases.

The reliability indices of the three object vessels against hull girder collapse are about 2.5 which is considered to be adequate considering the target value of the reliability index for merchant cargo vessels as previously noted. At the age of around 15 years the safety and reliability of the three vessels tends to become smaller than 90% of the newly built states. If repair and maintenance is not properly undertaken, the level of risks increases very rapidly.
Figure 40(d) Time-dependent reliability or risk (probability of failure) of the 105k dwt double hull tanker against hull girder collapse in sagging.

Figure 41(a) Time-dependent ultimate hull girder strength of the 170k dwt bulk carrier against hull girder collapse in hogging.

Figure 41(b) Time-dependent ultimate hull girder strength of the 170k dwt bulk carrier against hull girder collapse in sagging.

Figure 41(c) Time-dependent reliability or risk (probability of failure) of the 170k dwt bulk carrier against hull girder collapse in hogging.

Figure 41(d) Time-dependent reliability or risk (probability of failure) of the 170k dwt bulk carrier against hull girder collapse in sagging.

Figure 42(a) Time-dependent ultimate hull girder strength of the 254k dwt single hull tanker-type FPSO against hull girder collapse in hogging.
CONSIDERATIONS FOR REPAIR SCHEME

To efficiently keep the ship’s safety and reliability higher than a critical level, a proper and cost-effective scheme of repair and maintenance must be established (TSCF 1997). In this regard, some considerations for repair strategies of heavily damaged members are now made.

The IACS requires to keep the longitudinal strength of an aging ship at the level of higher than 90% of the initial state of new building. While the IACS requirement is in fact based on the ship’s section modulus, it is in the present study applied for establishing repair schemes so that the ultimate hull girder strength of an aging ship must be greater than 90% of the original ship even later in life.
Figure 43(a) Repair and the resulting time-dependent ultimate hull girder strength of the 105k dwt double hull tanker in hogging

Figure 43(b) Repair and the resulting time-dependent ultimate hull girder strength of the 105k dwt double hull tanker in sagging

Figure 43(c) Repair and the resulting time-dependent reliability index of the 105k dwt double hull tanker in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate)

Figure 43(d) Repair and the resulting time-dependent reliability index of the 105k dwt double hull tanker in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate)

Figure 43(e) Repair and the resulting time-dependent reliability index of the 105k dwt double hull tanker in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate)

Figure 43(f) Repair and the resulting time-dependent reliability index of the 105k dwt double hull tanker in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate)
Figure 44(a) Repair and the resulting time-dependent ultimate hull girder strength of the 170k dwt bulk carrier in hogging

Figure 44(b) Repair and the resulting time-dependent ultimate hull girder strength of the 170k dwt bulk carrier in sagging

Figure 44(c) Repair and the resulting time-dependent reliability index of the 170k dwt bulk carrier in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate)

Figure 44(d) Repair and the resulting time-dependent reliability index of the 170k dwt bulk carrier in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate)

Figure 44(e) Repair and the resulting time-dependent reliability index of the 170k dwt bulk carrier in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate)

Figure 44(f) Repair and the resulting time-dependent reliability index of the 170k dwt bulk carrier in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate)
Figure 45(a) Repair and the resulting time-dependent ultimate hull girder strength of the 254k dwt single hull tanker-type FPSO in hogging (Gulf of Mexico)

Figure 45(b) Repair and the resulting time-dependent ultimate hull girder strength of the 254k dwt single hull tanker-type FPSO in hogging (North Sea)

Figure 45(c) Repair and the resulting time-dependent ultimate hull girder strength of the 254k dwt single hull tanker-type FPSO in sagging (Gulf of Mexico)

Figure 45(d) Repair and the resulting time-dependent ultimate hull girder strength of the 254k dwt single hull tanker-type FPSO in sagging (North Sea)

Figure 45(e) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate, Gulf of Mexico)

Figure 45(f) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate, Gulf of Mexico)
Figure 45(g) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate, North Sea)

Figure 45(h) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in hogging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate, North Sea)

Figure 45(i) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate, Gulf of Mexico)

Figure 45(j) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate, Gulf of Mexico)

Figure 45(k) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (average corrosion rate, North Sea)

Figure 45(l) Repair and the resulting time-dependent reliability index of the 254k dwt single hull tanker-type FPSO in sagging and the critical ultimate compressive strength reduction percentage of heavily damaged members (severe corrosion rate, North Sea)
The renewal criterion of any structural member is based on the member’s ultimate strength rather than the plate thickness. This is because the latter approach, i.e., based on the percentage of member thickness loss, can not reveal the effects of fatigue cracking or local dent damage and even pit corrosion, while it may properly handle the thickness reduction due to uniform corrosion.

On the other hand, the former approach, i.e., based on member’s ultimate strength, is adequate to measure the reduced strength of damaged structures. In the former approach, therefore, any structural member category will be repaired if the percentage of its ultimate strength loss due to age related degradation and/or mechanical denting exceeds a critical value.

Therefore, the present work considers that heavily degraded (or damaged) members are renewed (or repaired) to their original state, immediately before the ultimate longitudinal strength of an aged ship becomes a value smaller than 90% of the original ship.

Figures 43 to 45 show time-dependent ultimate hull girder strength, reliability index and probability of failure for the three object vessels calculated after repair of heavily damaged structural members so that the ultimate hull girder strength must always be greater than 90% of the original state.

It is evident from Figures 43 to 45 that the structural safety and reliability of aging vessels can be controlled by proper repair and maintenance strategies. Repair criteria based on the member’s ultimate strength can be better controlled. It is also seen in the illustrations that the critical ultimate strength reduction percentage of structural members varies in the range of 2 to 5% of the original state. For double hull tanker and FPSO, the critical strength reduction percentage for repair increases with time, but it tends to decrease for bulk carrier.

CONCLUDING REMARKS

In the present study, a procedure for the time-dependent risk assessment of aging vessels taking account of general/pit corrosion, fatigue cracking and local denting damage has been established with focus on total loss by hull girder collapse. The procedure was then applied to the time-dependent risk assessment of a 103k dwt double hull tanker, a 170k dwt bulk carrier and a 254k dwt single hull tanker-type FPSO. Some considerations for developing repair schemes were made to keep the vessels’ ultimate longitudinal strength at an acceptable level.

It is concluded that the procedure developed in the present work will be useful for the damage tolerant design of new ship structures and also for establishing cost-effective repair schemes of existing aging ships.

ACKNOWLEDGEMENTS

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