ABSTRACT: The objective of the current work is to develop a risk based approach for structural design of a double hull Suezmax tanker. In particular, a cost benefit analysis is performed to assess the optimal safety index of the tanker. The recent developments of risk based approaches in the design of ship structures, as well as the work carried out by IMO on Formal Safety Assessment (FSA) are reviewed. The assessment of hull girder reliability is performed using IACS’ (International Association of Classification Societies) progressive collapse analysis to obtain the ultimate strength capacity of the midship cross section and First Order Reliability Methods (FORM) to obtain the probability of structural failure. A cost benefit analysis is performed considering accident scenarios where both property damage of the ship, pollution due to spillage of oil and loss of life of the ship’s crew are taken into account. Due to the recent developments in environmental criteria at the IMO level, special attention is given to the influence of the environmental criterion in the optimum safety level, as such two approaches were followed in the definition of this input variable: a constant value and a volume dependent approach. A sensitivity and uncertainty analysis is performed in order to assess the influence of the input variables of the model on the outcome, i.e. the optimum reliability index.

1. Introduction

As in any sort of activity, shipping is subjected to risk. However, in the case of the shipping industry, the consequences may prove to be more serious when compared to other industries. In particular, oil tanker accidents resulting in large oil spills that have occurred in the past led to major public attention and an international focus on finding solutions for minimising the risks related to such events, having significant influence on the developments of maritime standards and safety legislation, which have often been updated as a direct response to major accidents (Guedes Soares and Teixeira (2001)).

The way safety is being dealt with is changing, with the objective of balancing the elements that affect safety in a cost effective manner and throughout the life cycle of the ship. In this context, safety becomes a key aspect with serious economic repercussions. Rather than complying with prescriptive regulations as a primary concern, the safety level becomes the driving force in the design process. In the last two decades there has been an increasing tendency to adopt a more holistic and proactive approach to safety, with the introduction and development of Formal Safety Assessment (FSA) and Goal-based standards (GBS) at IMO.

As part of the FSA methodology, cost effectiveness analysis (CEA) and cost benefit analysis (CBA) allow the balancing of risk and costs in the design process, where risk encompasses property damages, environmental pollution and the human life. The objective of the current work is to develop a risk based approach for structural design of a double hull Suezmax tanker. Achieving this goal implied the assessment of the hull girder reliability and optimum safety level of the ship. The primary work focus is the development of a cost model to assess the optimum structural safety level of the Suezmax tanker using cost benefit analysis, with emphasis on environmental criteria, namely the Cost of Averting a Ton of oil Spill (CATS). In the analysis it was considered accident scenarios where both property damage of the ship, pollution due to spillage of oil and loss of life of the ship’s crew were taken into account.

Chapter 2 presents an introduction to the concept of risk based design and a review of studies carried out using this approach, particularly in the shipping industry. Chapter 3 focuses on structural reliability analysis, presenting the theoretical background and reliability methods and a review of structural reliability analyses is presented. In chapter 4 the focus becomes the case study of a Suezmax tanker, where the main characteristics of the ship to be analysed are presented, as well as the stochastic models and limit state function of the reliability problem specific to the case study. The hull girder ultimate bending moment is calculated using progressive collapse analysis and the ship’s reliability by using a first order reliability method. The ship’s reliability and probability of failure are presented as a function of a design modification factor, which quantifies a change in the scantlings of the deck structure in the midship cross section. In chapter 5 the cost model used in the cost benefit analysis is defined, explaining the costs involved and the parameters and basic random variables that are taken into account. The objective of the cost benefit analysis is to obtain the optimal safety level for the ship structure by minimizing the total expected costs.

The second part of chapter 5 is dedicated to analysing the influence of the input variables of the model in the final result, i.e. the optimum reliability index, through a sensitivity analysis. An uncertainty analysis is performed in chapter 6 using a Monte Carlo simulation. For this task the @Risk tool from Palisade is used. A new sensitivity analysis is performed considering the data for the Monte Carlo simulation. The second part of chapter 6 is dedicated to the study of a volume dependent environmental criterion, which is introduced in the previous model. The Monte Carlo simulation and sensitivity analysis are performed for this new assumption and the results are compared to the ones obtained using a constant value of CATS.

2. Risk based design

Until recently maritime safety had been primarily developed as a reaction to singular events, in the sense that safety regulation and design measures imposed by the International Maritime Organization (IMO) have resulted as a response to major catastrophic events. However, in the last years, the tendency has been to move from reactive and prescriptive regulations and adopt a more proactive and holistic approach, with goal-based regulations emerging (Guedes Soares et al (2010b)).

The way safety is being dealt with is changing becoming the driving force in the design process. Risk-based design (RBD) introduces risk analysis into the traditional design process, aiming at establishing safety objectives in a cost effective way. Risk is used to measure the safety level and the optimization of the design, alongside traditional objectives, such as earning potential, speed and cargo carrying capacity, encompasses a new objective: minimizing risk (Papanikolaou et al (2009))

RBD is a formalised methodology that integrates risk assessment in the design process with prevention/reduction of risk embedded as a design objective alongside “conventional” design objectives (Skjong et al (2005)).

Among the advantages that arise from the use of RBD is the allowance for innovative design by identifying the safety issues
of a new solution and proving that it is safer than or, at least, as safe as required. This is also true when it comes to optimising an existing vessel, either with the intent of increasing the safety level cost effectively or increasing earning potential maintaining the safety level.

In addition, by allowing innovation and facilitating cost-effective safety, it offers competitive advantage to the maritime industry, in the sense that it leads to technically sound design concepts and with a known safety level that is more likely to meet modern safety expectations.

2.1. Risk based design in the shipping industry

Risk-based approaches in the shipping industry started with the concept of probabilistic damage stability in the early 60's. In the late 1980’s the design of the cruise ship "Sovereign of the Seas" led to the development of alternative design and arrangement for fire safety, which later resulted in SOLAS II.2 Regulation 17.

In the early 90's a project, denominated SHIPREL, was carried out advocating the use of reliability theory in codes and proposed a reliability based format on ultimate strength (Guedes Soares et al (1996)). A network with the theme "Design for Safety", considered the first version of risk-based ship design, was established in 1997 to coordinate related European research projects. The network focused on integrating safety as an objective into the design process. Coordinated projects under this theme developed tools to predict the safety performance in accidental conditions (collision and grounding, fire, flooding, etc.). In addition, particular attention was given to the development of a new probabilistic damage stability assessment concept for passenger and dry cargo ships, which later formed the basis for the new harmonized damage stability regulations at IMO. The first significant project under the theme "Design for Safety" was denominated SAFER EURORO from 1997 to 2001.

In the beginning of the millennium, the Joint Tanker Project (JTP) developed new rules for the hull girder of oil tankers using SRA, which resulted in the Common Structural Rules (CSR) for tankers.

The project HARDER, which was carried out from 1999 to 2003, proposed new formulations for the probabilistic approach to damage stability by investigating the elements of the existing approach and taking into account enhanced probabilistic data. These regulations were adopted at the 80th MSC, in May 2005, and entered into force on January 1 2009. The SAFEDOR project lasted 4 years, starting in 2005, under the coordination of Germanischer Lloyd (GL) and was the first large scale project that developed elements of a risk-based regulatory framework, and corresponding design tools, for the maritime industry (Papanikolau et al (2009)).

2.2. Formal safety assessment

In 1993, during the 62th MSC, the UK Maritime and Coastguard Agency (MCA) proposed a standard five step risk based approach, which was called Formal Safety Assessment (FSA). In 1997 the MSC and MEPC adopted the Interim Guidelines on the application of FSA to the IMO rule-making process. The Guidelines for the FSA were approved in 2002 by the IMO and are now being applied in the Organization's rule making process (IMO (2002)). This represents a fundamental change from what was previously a largely reactive approach. At the 80th MSC, in May 2005, the FSA Guidelines were adopted a second time (IMO (2005)) and again in 2007 a new version including risk evaluation criteria and an agreed process for reviewing FSA's (IMO (2007b)). The last version of the FSA Guidelines was adopted at the 74th session of the MSC in July 2013 (IMO (2013)).

FSA is a structured and systematic methodology, aimed at enhancing maritime safety, including protection of life, the marine environment and property by using risk analysis and cost benefit assessment. The purpose of the FSA is to prevent a disaster by taking action before it occurs, making the FSA a proactive regulatory approach. This kind of integrated approach is based on risk evaluation and its process is transparent and logical, leading to an improvement in safety and environmental protection by appealing to the ship industry to comply with the maritime regulatory framework.

The FSA is used by IMO as a tool to help evaluate new regulations or to compare proposed changes with existing standards. Currently FSA consists of five steps:

1. Identification of hazards: a list of all relevant accident scenarios with potential causes and outcomes
2. Assessment of risks: evaluation of risk factors
3. Risk control options (RCOs): devising regulatory measures to control and reduce the identified risks
4. Cost benefit assessment: determining cost effectiveness of each risk control option
5. Recommendations for decision-making: information about the hazards, their associated risks and the cost effectiveness of alternative risk control options is provided

2.3. Cost benefit analysis and cost effectiveness analysis

A cost-effectiveness analysis (CEA) is performed in order to establish a maximum cost of an economically efficient safety measure. An optimum safety level is intended to reflect, in the present study, the costs associated with the reinforcement of the ship’s cross sections and its effects on the reduction of risk. In general, risk (R) is defined as the product of the frequency of an event (P) times the associated consequences (C). Different and distinct categories of risk include human life, environmental and material damage.

\[ R = P \cdot C \] (1)

Risk assessment is governed by fundamental principles, which are the basis for establishing risk acceptance. In particular, it is general practice in the maritime transport sector to combine the absolute criterion with the ALARP (As Low As Reasonably Possible) principle.

When establishing risk acceptance criteria, through the use of cost-effectiveness analysis, in the ALARP region, where the costs of implementing a safety measure and the expected benefits are estimated, the results of the analysis are typically expressed by a CAF (Cost of Averting a Fatality) index.

The common indices used to express the cost effectiveness of RCOs are the Gross CAF (GCAF) and the Net CAF (NCAF) as described in the FSA guidelines (IMO (2007b)) defined as:

\[ GCAF = \frac{\Delta C}{\Delta R} \] (2)

\[ NCAF = \frac{\Delta C - \Delta B}{\Delta R} = GCAF - \frac{\Delta B}{\Delta C} \] (1)

where \( \Delta C \) is the marginal cost of the RCO, \( \Delta R \) is the reduced risk in terms of fatalities averted due to the implementation of the RCO and \( \Delta B \) are the economic benefits of the RCO (e.g. economic value of reduced pollution). A cost-benefit analysis (CBA) can be performed instead of a cost-effectiveness analysis, in which the main difference lies on how the human loss is accounted for. Whereas in the CBA a monetary cost is directly attributed to the loss of human life, in a CEA this attribution is avoided by the use of a CAF index, namely the ICAF (Implied Cost of Averting a Fatality). The ICAF
is used in a CEA to justify the choice of a RCO from a set of measures evaluated through the analysis, while in a CBA the ICAF is included in the objective function and the solution of the problem results from the optimization of the expected costs (Teixeira (2007)).

2.4. Impacted cost of averting a fatality

The optimum ICAF value is derived from the social index LQI (Life Quality Index), which is defined as a function of the GDP per capita (g), the life expectancy at birth (e) and the percentage of time spent in an economic activity (w) is normally assumed to be 1/8 representing the amount of time spent in an economic activity).

\[ \text{LQI} = g^w e^{1-w} \]  
(2)

The use of the LQI criterion implies that a safety measure is preferred or should be implemented when \( \Delta \text{LQI} > 0 \):  
\[ \frac{\Delta e}{e} > \frac{\Delta g}{g 1-w} \]  
(3)

where \( \Delta e \) and \( \Delta g \) represent the variation of the life expectancy at birth and the GDP per capita, respectively, resulting from the safety measure.

Assuming that saving a human life corresponds to saving, in years, half the life expectancy of each individual, the optimum acceptable cost of saving an individual’s life is given by:

\[ |\Delta g| = \frac{g}{2} \left( 1 - \frac{w}{w} \right) \]  
(4)

The optimum total cost that should be invested in a hypothetical safety measure to avoid a fatality is given by:

\[ \text{ICAF} = \frac{ge}{4} \left( 1-w \right) \]  
(5)

2.5. Environmental criteria

Tanker ships, more than other types of ships, present the risk of environmental damage, consequently the need arose for the definition of an environmental risk acceptance criterion. The objective of this criterion in the cost benefit analysis performed in the present work is to estimate the expected costs associated with accidental spillage of oil.

Environmental impact from shipping transport may be caused by regular and accidental releases. Only accidental releases are taken into account in the risk assessment, as regular releases can be quantified without resorting to risk assessment. In the present analysis, the pollution caused by air emissions is not considered, as they are considered regular releases.

Cost of Averting a Ton of oil Spilt (CATS)

A cost-effectiveness evaluation criterion related to environmental protection was developed by Skjong et al (2005) denominated Cost of Averting a Ton of oil Spilt (CATS). The concept was introduced at IMO at the 81th MSC.

The Danish Maritime Authority (DMA) and Royal Danish Administration of Navigation and Hydrography (RDANH) proposed, in 2002, an environmental risk acceptance criterion, such that a risk control option (RCO) should be implemented if the cost of avoiding a spill is less than the cost of the spill times a factor F. Where F is larger than 1 and, more specifically, suggested to be between 1 and 3 (DMA & RDANH (2002)).

Skjong et al (2005) suggested the approximate value of 30 000 USD as the total cost associated with a ton of oil spill and a factor F equal to 2, leading to:

\[ \text{CATS} < 2 \times 30000 \text{ US$} \]  
(6)

Volume based environmental criteria

The cost associated with an oil spill is dependent on a number of factors, with emphasis on the type of product spilled, the geographical area were the spill occurred, the quality of the contingency plan and the management of salvage and cleaning operations (Kontovas et al (2010)).

At the 60th MEPC, a Working Group was formed, which after debate expressed its preference for a non-linear approach instead of a constant CATS threshold.

In July 2011, at the 62th MEPC, the work on CATS was completed and a spill sized dependent formulation was agreed (IMO (2011)).

The revised guidelines of the FSA adopted in 2013 include an environmental criterion dependent on the volume (V) of the spill (IMO (2013)). It was defined the Total Spill Cost (TSC) as follows, where the CATS criterion is equal to TSC divided by the spill volume:

\[ \text{TSC} = 627275V^{0.5993} \text{ for all spills} \]
\[ \text{TSC} = 42301V^{0.7233} \text{ for } V > 0.1 \text{ tons} \]  
(9)

The formulae were established considering data from the IOPCF, US and Norway. The costs are in 2009 US dollars and the spill size V is in tons.

3. Structural reliability analysis

The objective is to perform a CBA of a tanker ship in order to calculate the optimum structural reliability level, which includes the calculation of the probability of failure of the ship’s cross-section and the assessment of the consequences of the structural failure.

In general, a failure event can be described by a limit state function of the form:

\[ g(X) \leq 0 \]  
(10)

where X is a vector of basic random variables that influence the probability of failure. These variables describe uncertainties in loads, material properties, geometrical data and calculation modelling.

The probability of failure can be determined by:

\[ P_f = \int_{g(X)\leq0} f(x) dX \]  
(11)

where \( f(x) \) is the joint probability function of X. The analytical solution of this integral is, in most cases, not possible. This difficulty led to the development of approximate methods, starting with the development of First Order Second Moment methods (FOSM) by Cornell (1969), so called because the limit state function is represented by an approximation using a first order Taylor series.

3.1. First order reliability methods (FORM)

However the FOSM approach entails a problem denominated lack of variance, characterized by the fact that the reliability index changes when the limit state function is equivalent but not the same. This problem was identified by Ditlven (1973) and solved by Hasofer and Lind (1974) by transforming the independent and normally distributed variables into variables with normal distribution with mean value equal to zero and unitary standard deviation. Furthermore, the linearization should be around a design point instead of the mean value of the variables.
The probability of failure, after the transformation of the variables, is given by:

\[ P_f = \int_{g(X) \leq 0} f_X(x) dx = \int_{g(U) \leq 0} \varphi_u(u) du \]  

(12)

where \( \varphi_u \) – standard joint probability density function of the set (U) of standard normally distributed random variables (U).

\[ \varphi_u = \frac{1}{(2\pi)^{n/2}} \exp \left[-\frac{1}{2} ||u||^2 \right] \]  

(13)

Two properties of the \( \varphi_u \) function – rotational symmetry and exponential decay with \( ||u||^2 \) – allow the calculation of the probability of failure to be approximated by (Hasofer and Lind (1974)):

\[ P_f \approx \Phi(\beta) \]  

(14)

where \( u^* \) represents the design point obtained from minimizing \( ||u|| \) subject to \( g(u) = 0 \).\n
In the more general case, when the safety margin is nonlinear and the basic random variables are not normally distributed, several procedures have been made, consisting of nonlinear transformations in order to obtain a set of independent random basic variables with standard normal distributions (Rackwitz and Fiessler (1976), Hohenbichler and Rackwitz (1981), Ditlevsen (1981) and Der Kiureghian and Liu (1986)).

Whilst the FORM uses a linear approximation of the limit state function, in second order reliability methods (SORM) the limit state function is approximated by a second order surface. These methods were introduced by Fiessler et al (1979).

4. Hull girder reliability of a Suezmax tanker

Structural reliability methods have been commonly used in the assessment of the safety level of a ship’s hull girder as reviewed by Teixeira et al (2011). In the current project, the hull girder reliability problem is solved through a Progressive Collapse Analysis (PCA) proposed by the IACS Common Structural Rules (CSR) and the assessment of the safety level is performed considering as failure mode the ultimate longitudinal strength of the hull girder.

The structural reliability analysis is performed using the First Order Reliability Method (FORM) in order to obtain an estimate of the probability of failure of the ship structure. The hull girder reliability formulation is time independent and considers one year of the ship’s operation subjected to still water and vertical wave induced load effects.

During a one year period of a ship’s operation three types of loading conditions can occur – ballast, partial or full load – and the percentage of time in each condition may vary. In this analysis, only the sagging failures in full load condition were considered. This choice was based on several hull girder studies that concluded that, in oil tankers, the full load condition dominated the annual failure in sagging and the ballast load the annual failure probability in hogging (Guedes Soares et al (1996), Guedes Soares and Teixeira (2000), Parunov and Guedes Soares (2008)).

The reliability assessment was carried out considering two conditions of the hull girder: intact (gross scantlings) and corroded (net scantlings). In the intact condition the thicknesses of the elements of the midship cross section include the corrosion addition and in the corroded condition half the local corrosion addition is subtracted to the gross scantlings. The determination of the corrosion additions followed the IACS-CSR design rules for oil tankers (IACS (2010)).

4.1. Limit state function

The reliability problem is described by a limit state function of the form (Guedes Soares et al (1996), Guedes Soares and Teixeira (2000)):

\[ G_g(X) = x_{ul} M_u + x_{sw} M_{sw} + x_{vw} x_{gw} M_{vw} \]  

(15)

where \( M_u \) is the ultimate bending capacity of the cross section, \( M_{sw} \) is the still water bending moment in a given voyage, \( M_{vw} \) is the extreme vertical wave-induced bending moment. Associated to each moment are the model uncertainty factors: \( x_{ul} \) for the ultimate bending capacity, \( x_{sw} \) for the still water bending moment and \( x_{vw} \) and \( x_{gw} \) for the vertical wave-induced bending moment, in order to account for the uncertainty in the linear calculations and the nonlinear effects, respectively.

This state function describes the reliability problem for the ultimate collapse of the midship cross section relating the ultimate bending capacity of the cross section with the still water and wave induced bending moments. Only the vertical bending moments are considered due to the very small levels of horizontal bending moments. The \( x \) is given by the maximum value of the curve of resisting moment versus curvature obtained by the progressive collapse analysis (PCA) method. By definition, the failure of the midship cross section occurs when \( g(X) \leq 0 \). The vector of basic random variables \( X \) comprises explicitly the bending moments and the model uncertainty factors.

4.2. Description of the ship

The ship used as a case study is a Suezmax oil tanker with the main dimensions given in Table 1. The discretized midship section, as used in calculation of the ultimate bending moment, is shown in Figure 1.

<table>
<thead>
<tr>
<th>Main dimensions</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Length Overall, LOA</td>
<td>280.0</td>
<td>m</td>
</tr>
<tr>
<td>Length Between Perpendiculars, LPP</td>
<td>270.0</td>
<td>m</td>
</tr>
<tr>
<td>Moulded Breadth, B</td>
<td>48.20</td>
<td>m</td>
</tr>
<tr>
<td>Moulded Depth, D</td>
<td>23.00</td>
<td>m</td>
</tr>
<tr>
<td>Design Draught, T</td>
<td>16.00</td>
<td>m</td>
</tr>
<tr>
<td>Scantling Draught, Tsc</td>
<td>17.10</td>
<td>m</td>
</tr>
<tr>
<td>Block coefficient, Cb</td>
<td>0.83</td>
<td>-</td>
</tr>
<tr>
<td>Deadweight at Design Draught</td>
<td>152300</td>
<td>ton</td>
</tr>
<tr>
<td>Deadweight at Scantling Draught</td>
<td>166300</td>
<td>ton</td>
</tr>
</tbody>
</table>

Table 1 – Main dimensions of the Suezmax double hull tanker

Figure 1 – Midship cross section of the Suezmax double hull tanker discretized in stiffened plate and hard corner elements
4.3. Design modification factor

A design modification factor (DMF) was defined in order to calculate the effect of changing the area of the midship cross section on the ultimate bending capacity calculation. Since the analysis performed only considered the sagging failures, the DMF only represents the modification on the deck structure, as this is the most critical component, while keeping the scantlings of the sides, bulkheads and bottom structure constant. The DMF was calculated by the ratio between the area of the modified deck and the area with the original scantlings (Table 2).

The modification on the deck structure was achieved by increasing/decreasing the thickness of the plating and web and flange of the stiffeners in equal measure. It was also assumed that the spacing and number of stiffeners were constant.

<table>
<thead>
<tr>
<th>Change in thickness (mm)</th>
<th>Intact scantlings</th>
<th>Corroded scantlings</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Deck area (mm²)</td>
<td>DMF</td>
</tr>
<tr>
<td></td>
<td>Deck area (mm²)</td>
<td>DMF</td>
</tr>
<tr>
<td>-5</td>
<td>1.19E+06</td>
<td>0.75</td>
</tr>
<tr>
<td>-4</td>
<td>1.27E+06</td>
<td>0.80</td>
</tr>
<tr>
<td>-3</td>
<td>1.35E+06</td>
<td>0.85</td>
</tr>
<tr>
<td>-2</td>
<td>1.43E+06</td>
<td>0.90</td>
</tr>
<tr>
<td>-1</td>
<td>1.51E+06</td>
<td>0.95</td>
</tr>
<tr>
<td>0</td>
<td>1.59E+06</td>
<td>1.00</td>
</tr>
<tr>
<td>+1</td>
<td>1.67E+06</td>
<td>1.05</td>
</tr>
<tr>
<td>+2</td>
<td>1.75E+06</td>
<td>1.10</td>
</tr>
<tr>
<td>+3</td>
<td>1.83E+06</td>
<td>1.15</td>
</tr>
<tr>
<td>+4</td>
<td>1.91E+06</td>
<td>1.20</td>
</tr>
<tr>
<td>+5</td>
<td>1.99E+06</td>
<td>1.25</td>
</tr>
<tr>
<td>+6</td>
<td>2.07E+06</td>
<td>1.30</td>
</tr>
<tr>
<td>+7</td>
<td>2.15E+06</td>
<td>1.35</td>
</tr>
</tbody>
</table>

Table 2 – Design Modification Factor (DMF), corresponding deck cross sectional area and change in plate and stiffener thickness in relation to the original scantlings

4.4. Stochastic models of the bending moments

Still water bending moment

The model used for the still water bending moment follows Horte et al (2007a), which assumes that the still water bending moment \( M_{sw} \) is normally distributed, as proposed by Guedes Soares and Moan (1988), with a mean value of 0.7 times the maximum value in the loading manual and a standard deviation of 0.2 times the maximum value. The maximum sagging still water bending moment is \( M_{sw, max} = 2620.1 \text{ MN.m} \), as specified in the loading manual (Gaspar and Guedes Soares (2013)). The model uncertainty factor \( \chi_{sw} \) associated with \( M_{sw} \) is normally distributed with mean value of 1.0 and COV of 0.1.

Wave induced vertical bending moment

The extreme values of vertical wave-induced bending moment are described by a two-parameter Weibull distribution (Guedes Soares (1984) and Guedes Soares et al (1996)):

\[
F_{M_{sw}}(M_{sw}) = 1 - \exp \left[ -\left( \frac{M_{sw}}{w} \right)^k \right] \tag{16}
\]

with the shape parameter \( k=1.0 \), which is equivalent to assuming an exponential distribution of \( M_{sw} \). The scale parameter is determined to satisfy \( P[M_{sw} > M_{sw, max}] = 10^{-8} \) i.e. the probability of the occurrence of bending moments greater than the maximum vertical wave-induced moment is less than \( 10^{-8} \) (a generic small value). The scale parameter \( w \) is given by:

\[
w = \frac{M_{sw, max}}{\ln 10^8} \tag{17}
\]

The maximum vertical wave-induced bending moment was obtained, using the formula presented in the IACS CSR for double hull tankers (IACS (2010)).

The distribution for annual extreme values of vertical wave induced bending moments is then defined as a Gumbel model, with the parameters defined from the initial Weibull distribution:

\[
F_{M_{sw}}(M_{sw}) = \exp \left[ -\exp \left( \frac{M_{sw} - \theta}{\kappa} \right) \right] \tag{18}
\]

with \( M_{sw} \) a random variable that represents annual extreme values of the vertical wave-induced bending moment. The parameters of the Gumbel distribution were defined as Guedes Soares (1984):

\[
\lambda = w (\ln n_c)^{1/k} \tag{19}
\]

\[
\theta = w \frac{(\ln n_c)}{k}^{1/(k-1)} \tag{20}
\]

where \( n_c \) is the mean number of wave load cycles expected over the reference time period \( T_r \), for a given operational profile of the ship and mean wave period \( T_w \) representative of the sea state considered. An estimate for \( n_c \) can be given by 0.35T_w.

The model uncertainty factors \( \chi_{sw} \) associated with the wave-induced bending moment and \( \chi_{et} \) which accounts for the nonlinear effects were defined as a normally distributed with mean value 1.0 and a COV of 0.1 (Horte et al (2007a)).

In the present analysis the reference time considered was \( T_r=1.0 \) year and the mean wave period \( T_w=8.0 \) s. The period of operation of the ship in fully loaded condition was 0.35T_w.

Combination of still water and wave induced bending moments

The combination between the two bending moments of the hull girder is done by linear superposition. Following the Turkstra load combination rule (Turkstra (1970)), two load combinations could be considered:

a) An annual extreme value of the wave induced moment together with a random value of the still water moment

b) An annual extreme of the still water moment together with an extreme value of the wave moment during one voyage.

For the present analysis, taking into account the magnitude, the variability and the duration of the voyage, the combination (a) was found to be governing for the probability of failure (Horte et al (2007a)).

<table>
<thead>
<tr>
<th>Variable</th>
<th>Distribution</th>
<th>Mean value [MN.m]</th>
<th>Standard deviation [MN.m]</th>
<th>COV</th>
</tr>
</thead>
<tbody>
<tr>
<td>( M_{sw} )</td>
<td>Normal</td>
<td>1834.1</td>
<td>524.0</td>
<td>0.29</td>
</tr>
<tr>
<td>( M_{sw} )</td>
<td>Gumbel</td>
<td>5000.6</td>
<td>435.9</td>
<td>0.08</td>
</tr>
</tbody>
</table>

Table 3 – Probabilistic distributions of the still water and wave induced bending moments

4.5. Ultimate strength assessment

In the present work the ultimate bending capacity of the cross section \( M_u \) was obtained using the IACS’ progressive collapse analysis (PCA) (IACS (2010)). The PCA code developed by Gaspar and Guedes Soares (2013) is used.

The reliability assessment was performed considering the material properties and the thicknesses of the midship cross section as deterministic values. The stochastic models for each
variable are presented in Table 3. All basic random variables related to the hull girder strength were considered to be uncorrelated.

Following the conclusions of Guedes Soares (1988), the material properties to be considered in the PCA are the yield stress and the Young modulus. The uncertainties in the structural dimensions were modelled by normal distributions with mean value equal to the design value and coefficient of variation (COV) equal to 0.05. The uncertainty in the yield stress is represented by a lognormal distribution and the characteristic yield stress is defined as the 5th percentile of its distribution (Horte et al (2007a)).

Based on the studies on the uncertainty of the hull girder bending capacity performed in Harada and Shigemi (2007), the Young modulus was assumed to be a deterministic value.

Effect of the design modification factor on the ultimate strength capacity

The results of the PCA are presented in Figure 2, where the ultimate bending capacity $M_u$ is given as a function of the DMF, for the cases of gross and net original scantlings.

![Figure 2 – Ultimate bending moment as a function of the DMF for intact and corroded scantlings](image)

Reliability of the ship structure

The probability of failure was calculated using the COMREL software, which uses FORM for the calculation of the reliability of the ship structure. The output is presented in Table 4, where the reliability index ($\beta$) and the probability of failure ($P_f$) are given as function of DMF.

<table>
<thead>
<tr>
<th>Intact scantlings</th>
<th>Corroded scantlings</th>
</tr>
</thead>
<tbody>
<tr>
<td>DMF</td>
<td>$\beta$</td>
</tr>
<tr>
<td>0.75</td>
<td>3.42</td>
</tr>
<tr>
<td>0.80</td>
<td>3.73</td>
</tr>
<tr>
<td>0.85</td>
<td>4.01</td>
</tr>
<tr>
<td>0.90</td>
<td>4.28</td>
</tr>
<tr>
<td>0.95</td>
<td>4.53</td>
</tr>
<tr>
<td>1.00</td>
<td>4.76</td>
</tr>
<tr>
<td>1.05</td>
<td>4.99</td>
</tr>
<tr>
<td>1.10</td>
<td>5.20</td>
</tr>
<tr>
<td>1.15</td>
<td>5.40</td>
</tr>
<tr>
<td>1.20</td>
<td>5.59</td>
</tr>
<tr>
<td>1.25</td>
<td>5.76</td>
</tr>
<tr>
<td>1.30</td>
<td>5.91</td>
</tr>
<tr>
<td>1.35</td>
<td>6.06</td>
</tr>
</tbody>
</table>

Table 4 – Probability of failure and reliability index as a function of DMF

From the analysis of the results it can be noted a clear increase in the reliability of the ship with the increase of the DMF, and consequently the lower the probability of failure. This was to be expected since the higher the DMF the thicker the deck plates, i.e. the larger the deck area resisting the forces imposed on the hull girder.

5. Cost benefit analysis

The ship’s optimal safety level was assessed by performing a cost benefit analysis (CBA). The objective is to establish an optimum safety level for the Suezmax tanker ship, considering as a risk control option (RCO) the change in deck scantlings. The total expected cost is the sum of two distinct costs, one the cost associated with the structural failure of the ship and the other the cost of implementing a RCO. The first involves costs associated with property damage, environmental pollution and loss of human life, while the second involves the costs related to the construction of the deck structure, where the amount of work is connected with the amount of steel of the structure.

The methodology to obtain the optimum safety level, i.e. the optimum reliability index, followed project SAFEDOR’s cost effectiveness analysis of oil tankers (Horte et al (2007b)).

5.1. Cost model

The cost benefit analysis of the modification of the ship’s deck structure is given by:

$$ C_t^{\text{opt}} = C_{\text{fr}} + C_{\text{me}} $$

5.2. Target reliability index

A range of target reliability indexes, i.e. values of the reliability index ($\beta_t$) at year 25 of operation of the ship, considered, for the purposes of this analysis, as the last year of the ship’s operational life, were set between 1.5 and 5, with 0.5 spacing. The corresponding annual probability of failure ($P_f$) is given by:

$$ P_f = \Phi(-\beta_t) $$

where $\Phi$ represents the standard normal probability distribution.

From the results of the structural reliability analysis, the ratio between the annual probabilities of failure, associated with Net and Gross scantlings, for the original scantlings of the ship’s deck, is:

$$ r_f = \frac{P_f^{\text{net}}}{P_f^{\text{gross}}} = 31.86 $$

This value was used to obtain the annual probability of failure at year 0 of the ship’s operation, based on the target reliability index, as follows:

$$ P_f^{\text{gross}} = r_f P_f^{\text{net}} $$

The annual probability of failure for each year of operation of the ship (t), between 1 and 25, is given by:

$$ P_f = \Phi(-\beta_t) + (1 - \Phi(-\beta_t)) \left( P_f^{\text{gross}} - P_f^{\text{net}} \right) / 24 $$

5.3. Impressed cost of averting a fatality

The costs related to the loss of human life, in this case loss of life of the crew members, were accounted for using the implied cost of averting a fatality (ICAF).
Instead of using the value of 3 million USD as proposed by Skjong et al (2005), the value of ICAF used in the risk model was obtained from the average of OCDE countries during a period of 16 years starting from 1995 (OCDE (2014)). This represents an update of the value reported in Skjong et al (2005), given that the amount of information gathered was sufficient to obtain an estimate of the ICAF value, by considering the statistics in OCDE countries for the life expectancy at birth (e) and the gross domestic product (GDP) per capita (g) from 1995 to 2011.

\[
\text{ICAF} = 7 \cdot \frac{e \cdot g}{4}
\]  

(26)

The value of ICAF obtained was 3.85 million USD.

5.4. Cost of averting a ton of oil spill

In the present analysis it is considered a value of CATS of 60000 USD. A FSA study on crude oil tankers that used this criterion of 60 000 USD per ton was conducted by project SAFEDOR and submitted to IMO by Denmark (IMO (2008)). This criterion is representative of a spill of a generic oil product in a generic geographical area.

5.5. Cost of implementing a safety measure

In the present analysis, the cost of implementing a safety measure only accounts for the modification of the deck structure, more specifically the cost of steelwork. Depending on the DMF, the value of \( C_{\rho e} \) is positive or negative if DMF is larger or smaller than 1, respectively.

\[
C_{\rho e} = (\text{DMF}^{\beta_{t}} - 1) \cdot W_{\text{steel}} \cdot C_{\text{steelwork/ton}}
\]  

(27)

where \( W_{\text{steel}} = 1418.69 \) ton is the weight of the cylindrical body of the ship, assumed as 40% of \( L_{PP} \), \( C_{\text{steelwork/ton}} = 2500 \) USD is the cost of steelwork per ton of steel and \( \text{DMF}^{\beta_{t}} \) is the design modification factor associated with \( \beta_{t} \).

5.6. Cost associated with the structural failure of the ship

The cost associated with the structural failure of the ship includes the property costs, i.e. loss of the ship and loss of cargo, the environmental pollution costs, i.e. the clean-up costs associated with the spillage of oil, and costs associated with the loss of human life, i.e. the loss of life of crew members.

The cost associated with the structural failure of the ship is calculated considering the operational life of the ship \( T \), assumed to be 25 years for the present work, and an discount rate \( \gamma \) of 5%.

\[
C_{T,f}^{\beta_{t}} = \sum_{t=1}^{T} P_{f}(\beta_{t}) [C_{0}(t) + (C_{e} + C_{d} + C_{v})] e^{-\gamma t}
\]  

(28)

where \( P_{f} \) is the annual probability of failure, \( C_{0}(t) \) is the cost of the ship in the year \( t \), \( C_{e} \) is the cost associated with the loss of cargo, \( C_{d} \) is the cost of accidental spill and \( C_{v} \) is the cost associated with loss of human life.

Ship cost

The cost of the ship is a function of the ship’s age, as such the cost of the ship at year 0 is the initial cost of the ship and at year 25 is the scrapping value.

\[
C_{0}(t) = C_{m0} \cdot (C_{n0} \cdot C_{\text{scrap}})^{\frac{t}{25}}
\]  

(29)

where \( C_{m0} \) is the initial cost of the ship, \( C_{\text{scrap}} \) is the scrapping value of the ship and \( t \) is the year of operation (between 1 and 25 years).

The estimate for the initial ship cost was made using data compiled by UNCTAD between 2003 and 2010 on the basis of the data derived from Drewry Shipping Insight (UNCTAD (2011)). Using inflation rates – 2.9%, 2.2% and 1.6% - from OCDE statistics, the new building price was updated for the years 2011, 2012 and 2013, respectively (OCDE (2014)). The initial cost of the ship was considered to be 70.5 million USD. The scrapping value of the ship was obtained using the formula:

\[
C_{\text{scrap}} = C_{\text{steel/ton}} \cdot L_{\text{WT}}
\]  

(30)

with the cost of steel per ton \( C_{\text{steel/ton}} = 277 USD \) (MEPS (2014)).

Loss of cargo

The cost associated with the loss of cargo is calculated by considering a percentage of the total amount of cargo of the ship is spilled in case of structural failure of the ship, in this case taken as 20% (Sørgard et al (1999)).

\[
C_{c} = C_{\text{crude/ton}} \cdot D_{\text{WT}} \cdot P_{\text{spill}}
\]  

(31)

where \( C_{\text{crude/ton}} = 108 USD \) is the cost of a ton of crude oil (Bunkerworld (2014)) and \( P_{\text{spill}} \) is the percentage of cargo spilled in case of structural failure of the ship.

Accidental oil spill

In case of structural failure of the ship structure there is a 20% chance of oil being spilt, of that amount it is considered a 10% chance of the oil reaching the shoreline (Sørgard et al (1999)). This means that in case of an accidental oil spill, 10% of the spill will need to be “dealt with”, meaning it will need to be cleaned up, which will involve costs; costs that are here taken into account by considering the CATS criterion.

\[
C_{d} = P_{\text{spill}} \cdot P_{d} \cdot \text{CATS} \cdot D_{\text{WT}}
\]  

(32)

where \( \text{CATS} = 60000 USD \) is the Cost of Averting a Ton of oil Spilt and \( P_{d} = 10\% \) is the probability of the oil spill reaching the shoreline.

Loss of human life

In the cost benefit analysis the loss of human life is accounted for by including the ICAF in the objective function. Teixeira (2007) compiled data from the U.S. Department of Transportation from 2003, where the average number of crew members of an oil tanker of 110 to 175 DWT is 25 members. The probability of loss of crew was assumed to be 25%, as used in a study of a Suezmax tanker performed by SAFEDOR (Horte et al (2007)).

\[
C_{v} = n_{\text{crew}} \cdot P_{\text{crew}} \cdot \text{ICAF}
\]  

(33)

where \( n_{\text{crew}} = 25 \) is the number of crew members, \( P_{\text{crew}} = 25\% \) is the probability of loss of life of a crew member in case of an accident and \( \text{ICAF} = 3.85 \) million USD is the Implied Cost of Averting a Fatality.

5.7. Optimal reliability index

The optimum reliability index is obtained by minimizing the objective function (equation 21). The initial analysis was performed for target reliability indexes between 1.5 and 5, with an interval of 0.5, however, given the
result of the analysis and with the intent of increasing the precision of the value of βopt, within the previously described range of values for βopt, the values between 3 and 4.5 were taken in intervals of 0.02.

The optimum reliability index, using the new intervals, is shown in Figure 3, where βopt is 3.52, corresponding to the minimum of the curve of total expected cost (Ct).

5.8. Sensitivity analysis

In the present chapter a sensitivity analysis was carried out by increasing the value of each one of the input variables by 10%, while keeping the others constant, and obtaining the variation, in terms of percentage, of the reliability index. The results are presented in the form of a bar chart where the influence of the input variables is higher, the greater the corresponding bar, independently of negative or positive value. In order to better understand the influence of each input variable, model the equations obtained from a linear or quadratic regressions applied to data generated from parametric studies were developed. Using this approach, the methodology is the same: increasing the initial value of the input variables by 10% and evaluating the change in the optimum safety level. The results of the sensitivity analysis are presented in Figure 4. This approach allowed for the differentiation between the effects of each input variable in the optimization of the objective function.

The cost of cargo per ton (or crude oil per ton) yielded in both sensitivity analysis a 0% variation of the reliability index. In the present chapter the objective is to obtain the optimum reliability index considering the uncertainties in the input variables, meaning that rather than assuming a fixed value for a variable, each variable will be represented by a probabilistic model and the result of the optimum safety level will also be described by a probabilistic model reflecting the aspect of the uncertainties in the input variables.

6. Uncertainty analysis

In the present chapter the objective is to obtain the optimum reliability index considering the uncertainties in the input variables, meaning that rather than assuming a fixed value for a variable, each variable will be represented by a probabilistic model and the result of the optimum safety level will also be described by a probabilistic model reflecting the aspect of the uncertainties in the input variables.

6.1. Stochastic models

The stochastic models of the input variables are presented in Table 5. Three of the input variables – C_steelwork/ton, C_crude/ton and P_crew – were assumed to be deterministic (Det), given the results of the sensitivity analysis in chapter 5.

For the remaining variables, the stochastic models are based on triangular distributions (Triang) with the most likely value equal to the initial (fixed) value.

6.2. Monte Carlo simulation

The Monte Carlo simulation was performed considering 10000 iterations in the simulation. This provides a 95% confidence interval for the mean of [3.5700, 3.5742].

Optimum reliability index – Probability distribution

From the results of the Monte Carlo simulation, considering the probability density function of the optimum reliability index (Figure 5), the probability of βopt being between 3.40 and 3.76 is 90%. Furthermore, the minimum and maximum optimum
reliability index is 3.22 and 4.00, respectively. The sample has a mean value of 3.57 with a standard deviation of 10.6%.

**Sensitivity analysis**

The sensitivity analysis for the Monte Carlo simulation is obtained from @Risk, which uses regression coefficients to rank the influence of the input variables on the output. The regression coefficients are calculated by a process called stepwise multiple regression. The results of the sensitivity analysis are presented in tornado graphs, where the longer the bar (larger the coefficient), the greater the impact the input variable has on the output. A positive/negative coefficient indicates a positive/negative impact, i.e. increasing this input will increase/decrease the output. The coefficients listed in sensitivity report are normalized by the standard deviation of the output and the standard deviation of that input (Palisade (2014)).

![Figure 6 – Regression coefficients of the input variables (10000 iterations)](image1)

The sensitivity analysis (Figure 6) ranks the input variables from most influential to least. Noting that the cost of steelwork has a negative impact, meaning an increase of this variable leads to a decrease in the reliability index, whilst the other input variables have a positive impact on the output. Despite not being directly comparable, the results from this sensitivity analysis are congruent with the ones obtained from the previous analysis in chapter 5, as regards to the influence of the input variables, ranking the \( C_{\text{steelwork/ton}} \), \( P_{\text{split}} \), \( P_{\text{sl}} \) and CATS as the most influential and the ICAF, \( C_{\text{ICAF}} \), and \( n_{\text{crew}} \) as the least.

**6.3. Volume dependent CATS**

The cost model developed throughout the current project was based on a constant value of CATS, however given the importance of this variable in the outcome of the analysis and the recent developments on environmental criteria at the IMO level, the present chapter will consider a volume dependent environmental criterion.

The basis for establishing the CATS value was the regression formulae from the revised guidelines for FSA (IMO (2013)) presented in equation (9) where:

\[
CATS(V) = \frac{\alpha_{\text{Sc}}}{V} = \frac{67275V^{0.5893}}{V} \tag{34}
\]

with the spill size (V):

\[
V = DWT \cdot P_{\text{split}} \cdot P_{\text{sl}} \tag{35}
\]

The value of CATS was obtained by considering the initial values of the input variables \( P_{\text{split}} \) and \( P_{\text{sl}} \) of 20% and 10%, respectively. Resulting in a CATS of 2400 USD. In this chapter the stochastic model for the variable CATS is a triangular distribution with a most likely value equal to the initial value of 2400 USD and the minimum and maximum value equal to 1600 and 4000 USD. The interval was established by considering the same ratio minimum/most likely and maximum/most likely, respectively, as in chapter 5 for the input variable CATS.

**Uncertainty analysis considering a volume dependent CATS**

The results are presented in Figure 7 the probability density function of the optimum reliability index and in Figure 8 the sensitivity analysis. As before, it was considered 10000 iterations in the Monte Carlo simulation.

![Figure 7 – Probability density function of optimum reliability index (10000 iterations) (volume dependent CATS)](image2)

![Figure 8 – Regression coefficients of the input variables (10000 iterations) (volume dependent CATS)](image3)

**6.4. Constant CATS vs. volume dependent CATS**

The mean of the probability density function of the optimum reliability index in the case of a volume dependent CATS is approximately 10.6% lower than with constant CATS, noting that the minimum and maximum values and standard deviation are also lower. As for the sensitivity analysis, there is a clear decrease in the influence of CATS on the output. Furthermore the ranking of the influence of the input variables changed significantly, being the predominant variables the cost of steelwork per ton and the ICAF and the remaining variables having similar influence on the output. Considering the initial values of CATS – 60000 and 2400 USD – for a spill of 3326 tons (volume reaching the shoreline), the volume dependent CATS is 4% of the constant CATS, representing an obvious difference in costs. For the volume dependent CATS to be equivalent to the constant CATS the volume of the spill should be around 1.25 tons.

The formulae adopted by the IMO in step 4 of FSA (IMO (2013)) describe the CATS decreasing with the increase of oil spill size (equation 2.12), meaning the CATS is higher for smaller spills. Considering these formulae the constant CATS is clearly inadequate to deal with large spills, attributing a value
7. Conclusions

The objective of the current work has been to develop a risk based approach for structural design of a double hull Suezmax tanker. The assessment of the hull girder reliability was performed using Progressive Collapse Analysis (PCA) to obtain the ultimate strength of the hull girder and First Order Reliability Methods (FORM) to obtain the probability of failure of the hull girder.

In the risk based approach through cost benefit analysis to obtain the optimum safety level of the ship structure, a cost model was developed considering risk criteria, namely for human life and environmental pollution. The environmental pollution was only related to oil spills, in particular the definition of the CATS criterion. Furthermore the analysis only considered as risk control option (RCO) the change in the scantlings of the deck structure, more specifically the change in thickness of the deck plating and stiffeners, represented by a Design Modification Factor (DMF). The study of the effect of the DMF on the ultimate bending capacity of the midship cross section was carried out in chapter 4. From the results of the PCA, the ultimate bending capacity increases linearly with the increase of the DMF, i.e. the increase of plate and stiffener thicknesses. These results reflect the increase of the capacity of the midship cross section to support higher bending moments if the thickness of the plates and stiffeners is higher.

Also in chapter 4 the reliability of the hull girder was calculated and presented as a function of the DMF. The results show increasing reliability index with the increase of the DMF. For intact scantlings the range of values goes from a DMF of 0.70 corresponding to a reliability index (β) of 3.42 to a DMF of 1.35 corresponding to β = 6.06; for corroded scantlings the interval of DMF values starts at 0.72 corresponding to β = 2.41 to 1.39 corresponding to β = 5.54.

In chapter 5 a cost-benefit analysis was performed leading to an optimum reliability index of 3.52. Also a sensitivity analysis was performed for the cost-benefit model considering the input variables as deterministic values. The results of this analysis yielded the ranking of the input variables from higher influence on the output of the model to least influential: probability of oil spill in case of accident (Poil), cost of steelwork per ton (Csteelwork/ton), probability of spilled oil reaching the shoreline (Poi), Cost of Averting a Ton of oil Spilled (CATS), Implied Cost of averting A Fatality (ICAF), number of crew members (ncrew), initial ship cost (C0), cost of steel per ton (Csteel/ton), probability of loss of life of a crew member (Pcrew) and cost of cargo per ton (Crude/ton).

In chapter 6 Monte Carlo simulation is used considering the optimum reliability index as output variable and an uncertainty and sensitivity analyses are carried out. From the Monte Carlo simulation, the optimum reliability index has a 90% probability of being between 3.40 and 3.76.

From the results obtained from the sensitivity analysis it becomes clear that the definition of the CATS criterion is of great importance, given that the threshold value of CATS proposed by Skjong (2005) of 60000 US$ for the implementation of RCO’s is one of the determining factors on the structural safety level of the ship, while the volume dependent CATS criterion, being now applied in step 4 of the FSA, is considerably lower for spills greater than 5 tons (less than 50%), and therefore much less influential in the outcome of the model results.

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