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Prediction of Combined Long-Term Wave Induced Stresses and Corresponding Fatigue Damage in a Ship's Bottom Girder

Application of a Rationally Based Direct Calculation Method

Part 3

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ABSTRACT

This is the third and final report in a series presenting results from direct calculations of combined wave induced stresses in ship structural members. Long-term stress distributions in a bottom side girder of an OBO carrier have been calculated for different load conditions with the ship at service speed in the North Atlantic Sea. Fatigue analysis has been performed for different hot-spots and weld joints with the local geometrical stress concentrations and the correlation between normal stresses and shear stresses taken into consideration.

Key words:
wave induced stresses, long-term distributions, fatigue analysis, ship structural design

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CONTENTS

1 INTRODUCTION

This report represents the third part of a project aimed at investigating the combined wave induced stresses in ship hulls with respect to all simultaneous major low-frequency load components.

In the first part, ref.[1], a direct rationally based calculation method was introduced. Results from nominal stress calculations of different structural members at a hold amidships of a lo/lo containership were presented in the form of response functions for regular waves and irregular seas. In the second part, ref.[2], the calculation procedure was extended to include also long-term stress distributions based on ocean wave statistics, and fatigue analysis based on cumulative damage approach with SN-curves for standard classes of weld joints. Results from calculations of nominal stresses in a bottom girder and in side frames of an OBO-carrier were presented. The ship has a completely different girder arrangement in comparison with the lo/lo containership studied in the first part. The stress response functions for these two hull structures form a comprehensive material from which conclusions can be drawn about the relative importance and the combined effect of the various wave induced load components.

The main object of this third part is to investigate in detail the distribution of long-term stresses in a girder with local geometric stress concentrations and the combined effect of normal and shear stresses taken into consideration. An example is given of how fatigue analysis, based on these long-term local stress distributions, can be used to optimize the design and the position of critical structural details with high stress concentrations such as holes, bracket toes etc.

Rules for the design of ships do not yet include explicitly dimensioning with respect to fatigue. However, they do include safety factors in the allowed maximum stresses based on experience from ships in service including the effect of fatigue. In a deterministic static analysis it is not possible to incorporate the relevant interaction between different dynamic stress components. Therefore it is difficult, or in fact impossible, to predict the actual fatigue damage distribution in the structure of a ship in service. With the dynamic semi-probabilistic approach introduced in this project, long-term stress distributions anywhere in the structure can be calculated, and hence fatigue damage estimates as well as ultimate strength analysis can be directly incorporated in the structural detail design. This is of special importance when high strength steels and novel structural designs are introduced.

In recent years, a large number of serious fatigue cracks have been discovered in modern large tankers, after only a few years in service. These ships have weight- and cost-optimized structures with a frequent use of high strength steel. This experience indicates that fatigue design has become a necessity if nominal dynamic stress levels are to be kept at the present high level.

2 SUMMARY OF CALCULATION PROCEDURE

2.1 *Wave induced stresses*

Wave induced stresses have been calculated with the general program WAIST based on strip-calculations of ship motions, global hull girder bending moments and shear forces, and local hydrodynamic pressure on the submerged ship hull. The calculation method is described in more detail in refs.[1], [2], [3].

All wave load components are in this study assumed to be linear with respect to the wave height and harmonic with the same frequency as the encountering waves. The combined nominal stress response in regular waves at a certain stress position *i* is generally calculated according to

$$\sigma_{c_i} = \sigma_{g_i} + \sigma_{p_i} + \sigma_{m_i} \tag{2.1}$$

where

$\sigma_{g_i} = \sigma_{g0_i} \cos(\omega_e t + \varepsilon_{\sigma g_i})$ (global hull girder stresses)

$\sigma_{p_i} = \sigma_{p0_i} \cos(\omega_e t + \varepsilon_{\sigma p_i})$ (local pressure induced stresses)

$\sigma_{m_i} = \sigma_{m0_i} \cos(\omega_e t + \varepsilon_{\sigma m_i})$ (local mass force induced stresses)

Stress components are calculated from harmonic wave loads with use of influence coefficients C_{xi} representing the stress at position i per unit wave load component x.

The global stress component is in the general case determined from hull girder bending moments and shear forces according to eq.(2.2) for normal stress, and eq.(2.3) for shear stress.

$$\sigma_{g_i} = C_{Mx_i} TM_{x_i} + C_{My_i} BM_{y_i} + C_{Mz_i} BM_{z_i} \tag{2.2}$$

where

$TM_{x_i} = TM_{x0_i} \cos(\omega_e t + \varepsilon_{TMx_i})$ (torsional moment)

$BM_{y_i} = BM_{y0_i} \cos(\omega_e t + \varepsilon_{BMy_i})$ (vertical bending moment)

$BM_{z_i} = BM_{z0_i} \cos(\omega_e t + \varepsilon_{BMz_i})$ (horizontal bending moment)

$$\tau_{g_i} = C_{Tx_i} TM_{x_i} + C_{Ty_i} T_{y_i} + C_{Tz_i} T_{z_i} \tag{2.3}$$

where

$T_{y_i} = T_{y0_i} \cos(\omega_e t + \varepsilon_{Ty_i})$ (horizontal shear force)

$T_{z_i} = T_{z0_i} \cos(\omega_e t + \varepsilon_{Tz_i})$ (vertical shear force)

The local normal or shear stress response from hydrodynamic pressure is calculated from the sum of pressures at various positions *j* multiplied by the influence coefficients representing the stress at position *i* per unit pressure at position *j*.

$$\sigma_{p_i} = \sum_j [\, C_{p_{ij}} \, p_j \,] \qquad (2.4)$$

where

$$p_j = p_{0_j} \cos(\, \omega_e \, t + \varepsilon_{p_j} \,) \qquad \text{(hydrodynamic pressure)}$$

The local normal or shear stress response from internal mass forces is calculated in a similar way from the sum of mass forces at various positions *k* multiplied by the influence coefficients representing the stress at position *i* per unit mass force at position *k*.

$$\sigma_{m_i} = \sum_k [\, C_{mx_{ik}} F_{mx_k} + C_{my_{ik}} F_{my_k} + C_{mz_{ik}} F_{mz_k} \,] \qquad (2.5)$$

where

$$F_{mx_k} = F_{mx0_k} \cos(\, \omega_e \, t + \varepsilon_{Fmx_k} \,) \qquad \text{(mass force in x–direction)}$$

(etc for y– and z–directions)

For liquid cargo in tanks, the mass force components includes coupling terms from accelerations in the *x, y, z*-directions, dependent on the position *k* within the tank (see p.9 in ref.[2]).

The combined local effect of normal and shear stresses in a plate subjected to in-plane stresses is usually related to the maximum principal stress

$$\sigma_{max} = \max \left[\, \left| \frac{\sigma_x + \sigma_y}{2} \pm \sqrt{\left(\frac{\sigma_x - \sigma_y}{2} \right)^2 + \tau_{xy}^2} \right| \, \right] \qquad (2.6)$$

or to an equivalent effective stress such as

$$\sigma_e = \sqrt{\sigma_x^2 + \sigma_y^2 - \sigma_x \sigma_y + 3\tau_{xy}^2} \qquad \text{(von Mises yield criterion) (2.7a)}$$

or

$$\sigma_e = \sqrt{(\sigma_x - \sigma_y)^2 + 4\tau_{xy}^2} \qquad \text{(Trescas yield criterion) (2.7b)}$$

Because of the phase lag between nominal stress components, neither the principal nor the equivalent stresses according to eq.(2.7) can generally be used in a linear frequency based analysis of the stress response in a short-term irregular sea. However, in most cases the critical local "hot-spot" stress direction is known. At the free edge of a cut-out in a plate for example, there are no shear stresses, and no stresses perpendicular to the edge. The tangential stress is then equal to the principal stress and can be described as a linear function of the harmonic nominal normal and shear stresses in the plate, eq.(2.8). Normally only one nominal normal stress component is of significance for a ship structural member, e.g. the longitudinal normal stress in a longitudinal girder web.

$$\sigma_{hs} = SCF_{\sigma_x} \, \sigma_{x_{nom}} + SCF_{\sigma_y} \, \sigma_{y_{nom}} + SCF_{\tau_{xy}} \, \tau_{xy_{nom}} \qquad (2.8)$$

2.2 Long-term stress distribution

A stationary short-term irregular sea state can usually be described as a relatively narrow-banded random process made up of a large number of harmonic wave components with different frequencies and amplitudes and with random phases. The characteristics of the sea state is described by the wave spectrum $S_w(\omega)$, (spectral density function) from which the average zero-crossing period T_Z and significant wave height H_S can be established. For a linear response, the cumulative probability of stress response amplitudes follows approximately a Rayleigh-distribution

$$F(\sigma) = 1 - e^{-(\sigma^2/R_\sigma)} \tag{2.9}$$

with

$$R_\sigma = 2\int_0^\infty S_w(\omega)\, T_\sigma(\omega)^2\, d\omega \tag{2.10}$$

where

R_σ = Rayleigh parameter for stress amplitudes

$T_\sigma(\omega)$ = linear transfer function of stress response

The probability distribution of stress ranges (peak to trough) follows a Rayleigh-distribution with the parameter R_σ equal to 4 times the parameter for single amplitude stress levels.

In a simple wave spectrum formulation such as the 2-parameter Pierson-Moskowitz spectrum, the properties of the sea state are uniquely described by H_S and T_Z. The linear transfer function of stress response is further a function of the relative wave heading angle, as well as of the ship's load condition and speed. The long-term probability of exceeding a certain stress level can therefore, in the general case, be calculated by summation of short-term probabilities of exceedance in all possible combinations of mean periods, significant wave heights, heading angles, load conditions, and speeds.

$$Q_{LT}(\sigma) = \sum_i \sum_j \sum_k \sum_l \sum_m p(H_{si}, T_{zj})\, p(\beta_k)\, p(LC_l)\, p(V_m)\, (1 - F_{ijklm}(\sigma)) \tag{2.11}$$

where

$Q_{LT}(\sigma)$ = long–term probability of exceedance

$p(H_s, T_z)$ = long term joint probability of significant wave heights and mean periods from wave statistics

$p(\beta)$ = relative probability of heading β

$p(LC)$ = relative probability of load condition LC

$p(V)$ = relative probability of speed V

$F_{ijklm}(\sigma)$ = short–term cumulative probability distribution

For design purposes it is often assumed that the different probabilities included in the summation are independent of each other. This simplification is not obvious since both load condition and sea state might have a significant influence on the speed. When extreme responses are evaluated speed reduction should be taken into account. However, for a fatigue analysis most of the fatigue damage occurs at relatively moderate sea states, and the influence of speed reduction is of minor importance.

By application of eq.(2.11) for a number of different stress levels, the long-term probability distribution of exceedance can be established. This distribution is usually well represented by a continuous 2-parameter Weibull-distribution from which the most probable largest stress level among a large number of stress cycles can be obtained.

$$Q_{LT}(\sigma) = e^{-(\sigma/B)^h} \tag{2.12}$$

where

B, h = parameters of the Weibull probability distribution

The two parameters B and h in the Weibull distribution can be estimated from regression analysis of the actual calculated probabilities according to eq.(2.11). However, in ref.[2] it is shown that if only one single Weibull distribution is used to represent the whole range of stress levels during a ship's service life, it tends to overestimate the probability of exceedance at low stress levels (with $Q > 10^{-3}$) as well as at very high stress levels ($Q < 10^{-7}$). A better representation of long-term probabilities over the whole stress range can be achieved by a combination of two Weibull-distributions according to

$$\begin{aligned} Q_{LT}(\sigma) &= e^{-(\sigma/B)^h} && Q \geq 10^{-4} \\ Q_{LTe}(\sigma) &= 10^{-4}\, e^{-((\sigma-\sigma_*)/B_e)^{h_e}} && Q < 10^{-4} \end{aligned} \tag{2.13}$$

where

σ_* = $B\,(-\ln(10^{-4}))^{1/h}$ is the knuckle point stress

B_e, h_e = parameters of the second (extreme) Weibull distribution

2.3 Fatigue analysis

The method used for fatigue analysis in this study follows DnV Classification Notes on Fatigue Strength Analysis for Mobile Offshore Units, ref.[4]. The method is based on SN-curves obtained from experiments on different weld joint classes. The design curves are given as the mean minus two standard deviation curves for experimental data and are thus associated with a 2,3% probability of failure. Fatigue life is determined by the general Miner-Palmgren hypothesis for linear cumulative damage.

$$D = \sum_{i=1}^{k} \frac{n_i}{N_i} \tag{2.14}$$

where

D = cumulative damage ratio

k = number of stress range levels σ_i

n_i = number of stress cycles at level σ_i

N_i = number of cycles to failure at constant stress range σ_i

Design SN-curves for fatigue lifes under constant amplitude loading are given for the different weld joint classes in the following form

$$\log N = \log a - 2 \log s - m \log \sigma = \log \bar{a} - m \log \sigma \qquad (2.15)$$

For joints exposed to sea water but with cathodic protection, a fatigue cut-off stress level (fatigue limit) S_0 at $N = 2 \cdot 10^8$ is introduced, below which no fatigue damage is assumed to occur. Basic data for SN-curves are presented in table 2.1 and fig.2.1 below.

Class	Log a	Logs	Log ā	m	S_o (MPa)
B	15.3697	0.1821	15.01	4.0	48
C	14.0342	0.2041	13.63	3.5	33
D	12.6007	0.2095	12.18	3.0	20
E	12.5169	0.2509	12.02	3.0	18
F	12.2370	0.2183	11.80	3.0	15
F2	12.0900	0.2279	11.63	3.0	13
G	11.7525	0.1793	11.39	3.0	11
W	11.5662	0.1846	11.20	3.0	10
T	12.6606	0.2484	12.16	3.0	19

Table 2.1 Details of basic SN-curves - Sea water and cathodic protection, ref.[4]

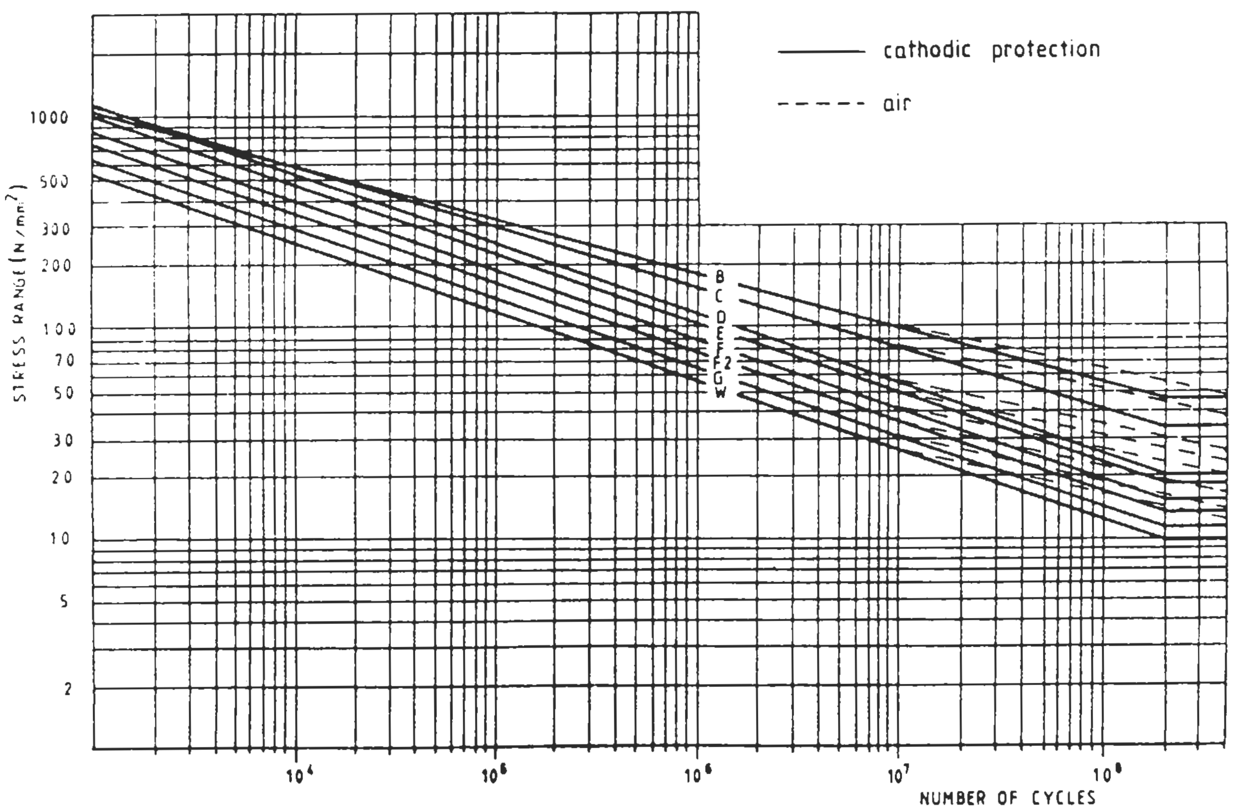


Fig.2.1 SN design curves based on mean minus two standard deviations, ref.[4]

If the cut-off level is neglected and the long-term probability of stress ranges is represented by a single Weibull distribution, the cumulative damage can be calculated analytically

$$D = \frac{n}{a} B^m \Gamma\left(1+\frac{m}{h}\right) \tag{2.16}$$

where

n = total number of stress cycles

$\Gamma(\ )$ = the complete gamma function

This expression can be used to relate the fatigue design criteria $D < 1$ to the most probable largest extreme stress amplitude during the ship's service life (often refered to as $n = 10^8$ stress cycles) as shown in fig.2.2.

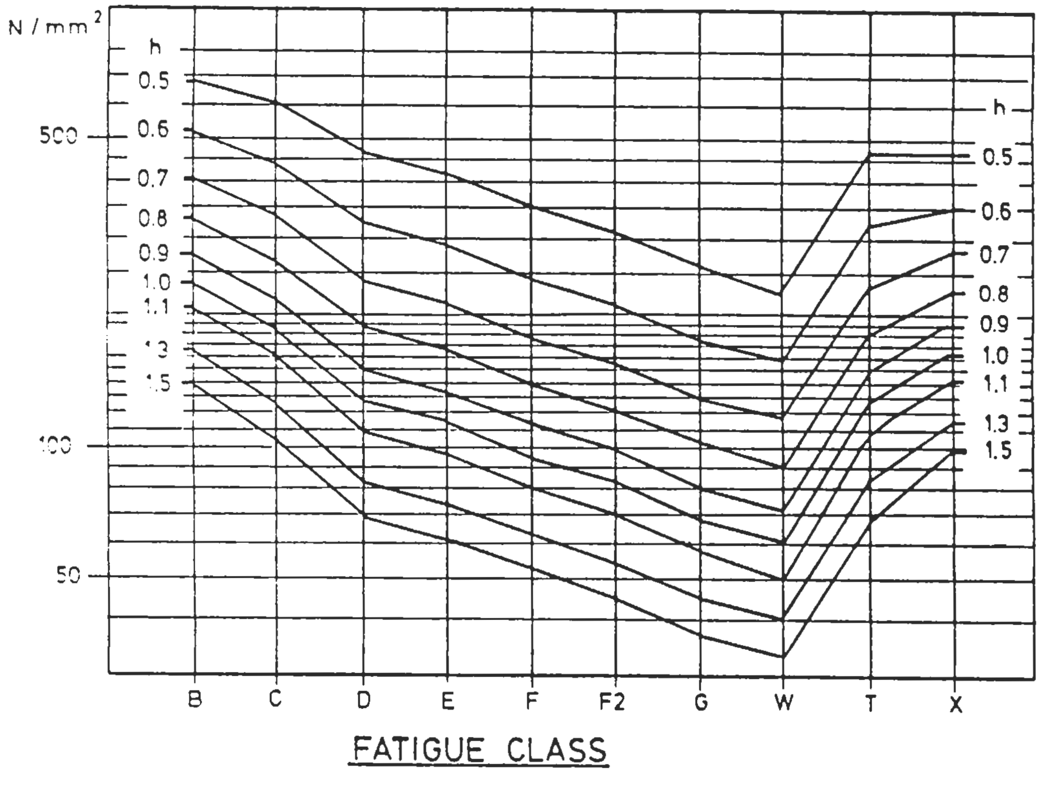


Fig.2.2 Allowable extreme stress amplitude (20 years), ref.[4]

3 DETAILED ANALYSIS OF A BOTTOM SIDE GIRDER

3.1 Ship structure and load cases

This chapter presents results from a detailed analysis of the wave induced long-term stress distribution in a double bottom side girder.

The studied structure is from an OBO carrier designed and built by Uddevallavarvet in 1984. The ship was built to DnV Class 1.A1, Bulk Carrier HC/E or Tanker for Oil - COW, INERT, PST, E0. Main particulars and profile are shown in fig.3.1. The midship section with scantlings is shown in fig.3.2.

Nominal stress distributions along the bottom of the girder and in the side frames in four cargo holds, have previously been analysed in ref.[2] for a full load oil cargo condition. The highest combined normal stress levels were found to occur in Hold 4, and this has been chosen for the detailed analysis presented here.

Main Particulars:

Length over all	207,00	m
Length between perpendiculars	200,00	m
Breadth, moulded	32,23	m
Depth, moulded to upper deck	17,35	m
Design draught	11,58	m
Scantling draught	12,65	m
Deadweight at scantling draught	54600	mton
Trial speed at design draught	16,6	knots
Service speed 50% scantling/50 % ballast draught, 12% sea margin on power	15,5	knots

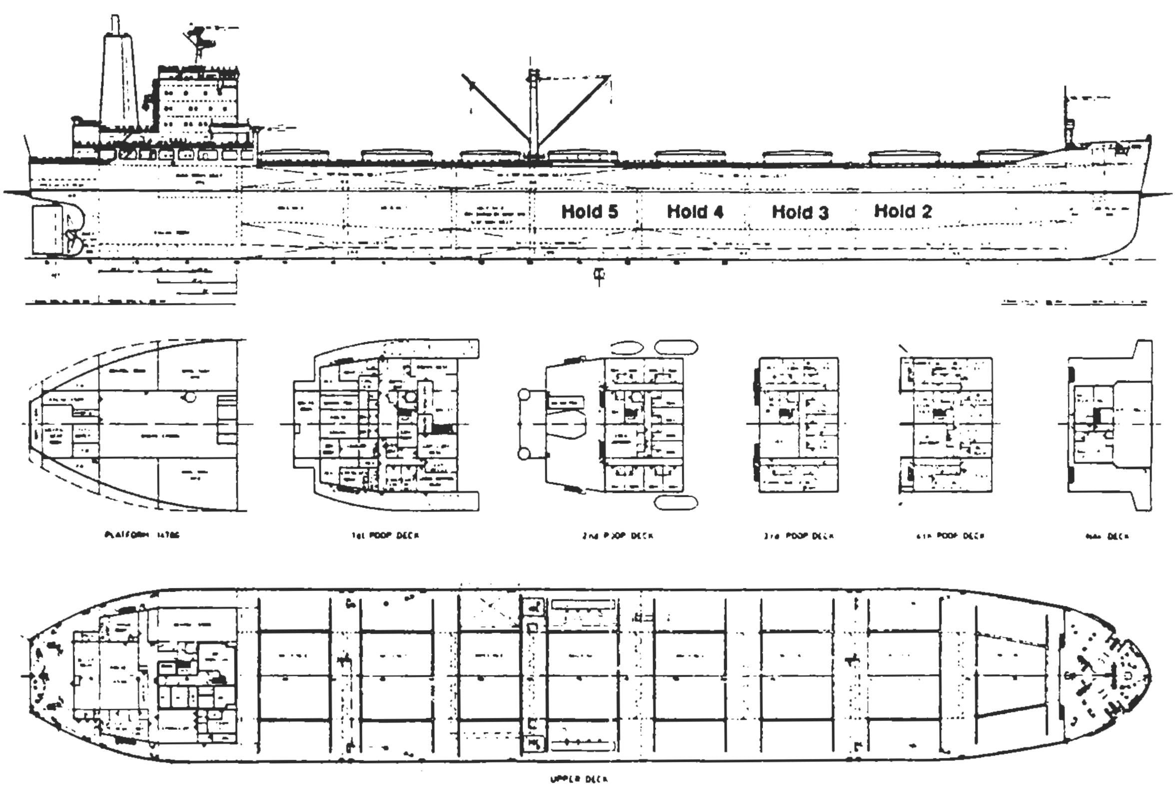


Fig.3.1 Uddevallavarvet 55 000 dwt OBO carrier, main particulars

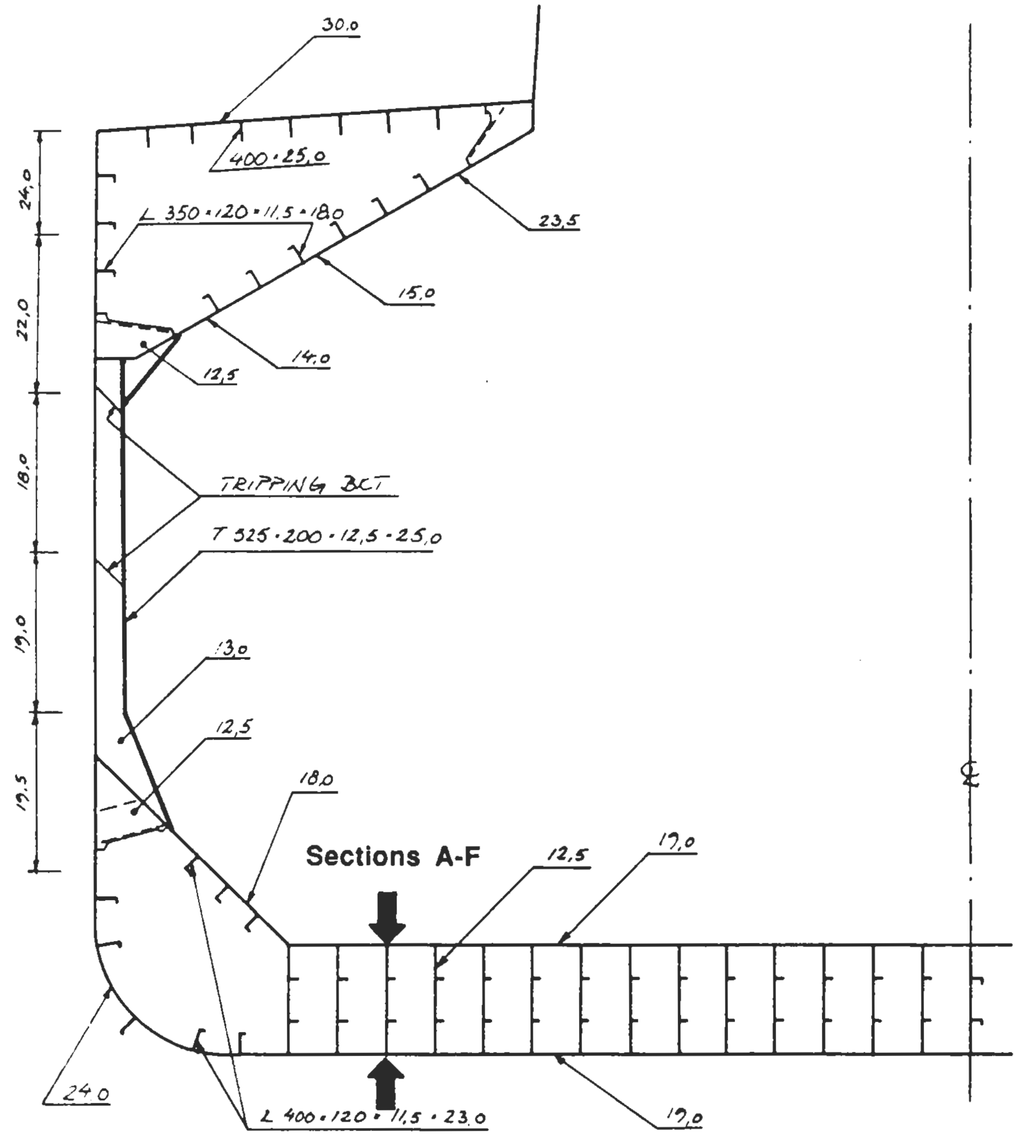


Fig.3.2 Midship section

Three different load cases have been considered in this study. Two of them are full load conditions, oil and ore respectively, at scantling draught and even keel. The third is a ballast arrival condition. In the full load conditions, the oil cargo is evenly distributed along the hull girder, while the ore cargo is distributed in five of the eight holds, see fig.3.3-3.5.

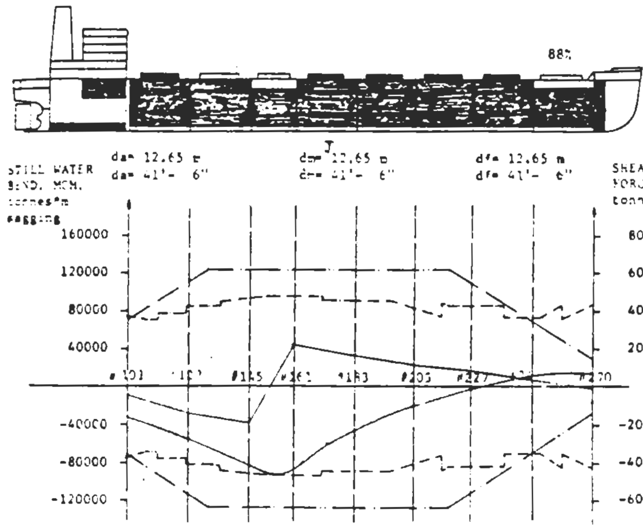


Fig.3.3 Load case 1: Full load oil cargo condition with distribution of still water bending moment and shear force. From the Trim and Stability Book.

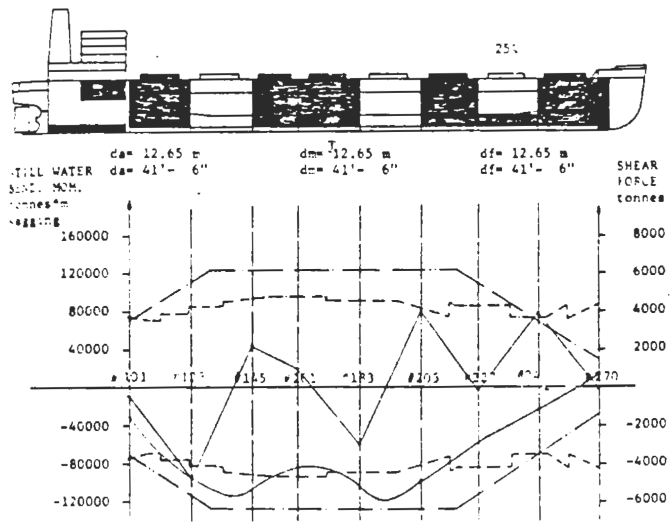


Fig.3.4 Load case 2: Full load ore cargo condition

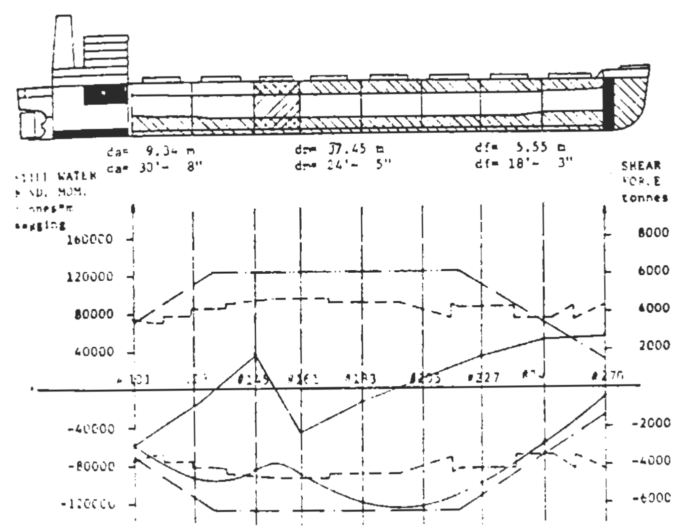


Fig.3.5 Load case 3: Ballast arrival condition

Normal and shear stress responses to global hull girder bending moments and vertical shear forces were calculated directly with use of the midship sectional properties. Stresses from global torsional moment and horizontal shear forces were considered negligible and not included in the model. Local stress influence coefficients for external hydrodynamic pressure and for internal mass forces were calculated with a similar 2-dimensional FE-model as used in ref.[2]. The model was assembled with membrane elements for the girder plating, truss elements to model inner- and outer bottom, and beams to model the bulkheads, fig.3.6. The 2-dimensional model was in this particular case sufficient because the bottom structure has no transverse floors between the bulkheads. External pressure distribution was applied at five different stations along the model implying five unit load cases (instead of four as in [2]). In each load case, the pressure was distributed to the neighbouring stations triangularly, and the sum of all unit load cases thus equals a constant unit pressure on the bottom. Influence coefficients for mass forces from the cargo were calculated using the influence coefficients for pressure but scaled to correspond to a unit mass force.

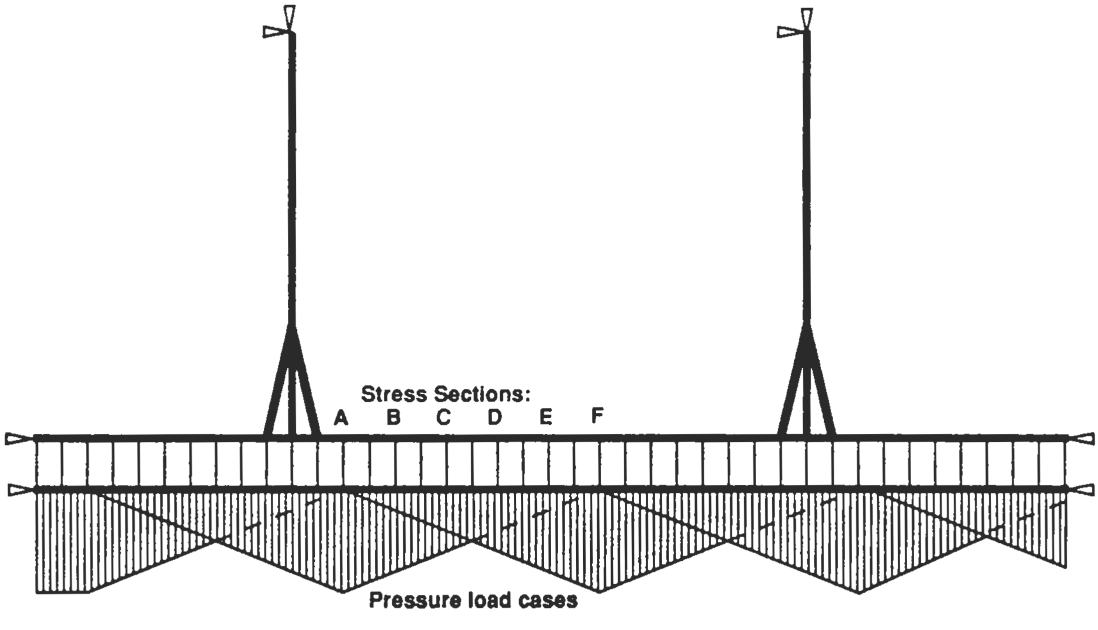


Fig.3.6 FE-model of bottom side girder

3.2 *Long-term nominal stress distribution*

3.2.1 *Classification Rules*

The vertical wave bending moment amidships according to DnV Rules for Classification of Steel Ships, [5], is given by the formula

$M_{w0} = f_{wm}\, C_w\, L^2\, B\, (C_B + 0{,}7)$ kNm, with f_{wm} = 0,063 in sagging and = 0,057 in hogging. For the present ship, C_w = 9,75 gives:

$M_{w0,sagging}$ = 1188 MNm, $M_{w0,hogging}$ = 1075 MNm

According to the Rules, these are design values at the probability level 10^{-8} divided by 1,7.

The minimum design stillwater bending moment amidships is given in a similar form

$M_{s0} = f_{sm}\, C_w\, L^2\, B\, (C_B + 0{,}7)$ kNm, with f_{sm} = 0,072 in sagging and = 0,078 in hogging.

The sum of vertical wave bending and still water bending moments leads to the section modulus requirement about the transverse neutral axis

$Z = (M_s + M_w) / \sigma_L$, where σ_L is a longitudinal hull girder design stress = 135 f_1 MPa

For normal ship steel f_1 = 1,0 and for a HS-steel as e.g. NV-36, f_1=1,39.

Although the ship rules do not explicitly include fatigue, the section modulus requirement can be used for comparison with the fatigue criteria in offshore rules. 44,4% of the section modulus requirement (mean value of hogging and sagging) is based on wave bending moment. This corresponds to a maximum allowed wave bending stress amplitude at probability level 10^{-8} of $1{,}7 \cdot 0{,}444 \cdot 135 f1 = 102 f_1$ MPa.

According to fig.2.2 and assuming a Weibull shape parameter h = 0,95, this maximum wave bending stress allows for a fatigue class F for normal steel, and class D for NV-36 steel in the strength deck without local stress concentrations taken into account.

The present ship has normal ship steel in the deck and NV-36 steel in the double bottom structure. The sectional modulii are Z_{deck} = 19,29 m^3 and Z_{bottom} = 27,98 m^3 which is above the Rule minimum modulus for normal ship steel Z_{min} = 18,85 m^3.

The wave bending stress range in bottom at probability level 10^{-8}, according to the Rule formula for vertical wave bending moment, becomes $\Delta\sigma_{bottom} = 1{,}7 \cdot (1188 + 1075) / 27{,}98$ = 137,5 MPa. This value is to be compared with the results from direct calculations shown below.

3.2.2 *Direct calculation*

Long-term distributions have been calculated directly for nominal normal stresses, for nominal shear stresses, and for hot-spot stresses. All long-term calculations are based on wave statistics from the North Atlantic Area 16, fig.3.12, from ref.[6]. This ocean area is considered rather severe in comparison with world wide statistics. In the summation according to eq.2.11, all different wave headings were assumed to occur with the same probability, and the ship speed was assumed to be kept constant at 15 kn. Calculations were carried out separately for all the three load conditions mentioned in 3.1.

The highest wave induced stresses where found to occur for the homogeneous oil cargo condition. Fig.3.7 shows the longitudinal distribution of the most probable extreme normal stresses among $n = 10^8$ cycles, along the bottom of the girder. For this load condition, the middle of Hold 4 shows the highest stress levels from vertical bending moment and from total combined global and local wave loads.

The detailed distributions of most probable largest stress ranges for the three different load cases are shown in fig.3.8 for normal stresses, and fig 3.9 for shear stresses. The 2-dimensional stress distributions have been obtained by interpolation. For normal stresses, interpolation was made between total combined stresses at tank top, combined global hull girder stresses in the local neutral axis of the girder, and total combined stresses at bottom. For shear stresses, the distribution was interpolated between total combined stresses at tank top and at bottom.

The direct calculated wave induced stress component from vertical hull girder bending is for all three load cases significantly larger then 137,5 MPa calculated from the Rules formula above. For the oil condition, the extreme stress range from vertical bending moment is 222 MPa, for the bulk condition 188 MPa, and for the ballast condition 200 MPa.

The calculation method for global loads, used in the WAIST program, system has recently been compared to full scale measurements in short-term head seas for a production ship with similar main dimensions as the present OBO carrier, ref.[7]. The comparison shows very good agreement between calculated and measured vertical bending moment response. Also in ref.[8] is reported a comparison between direct probabilistic analysis of vertical dynamic bending moments and Rule moments for a production ship. The comparison indicates that the direct analysis based on 20 years continuous North Atlantic service in head seas gives about 40% higher moments than the Rule formula for a ship length of 200 m.

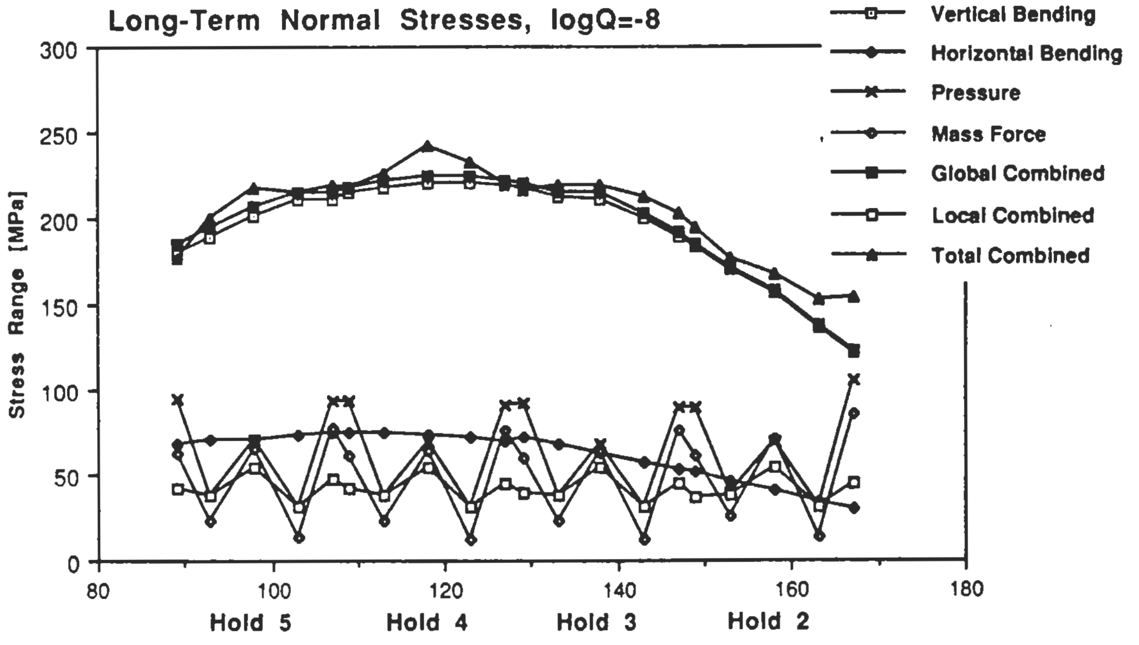
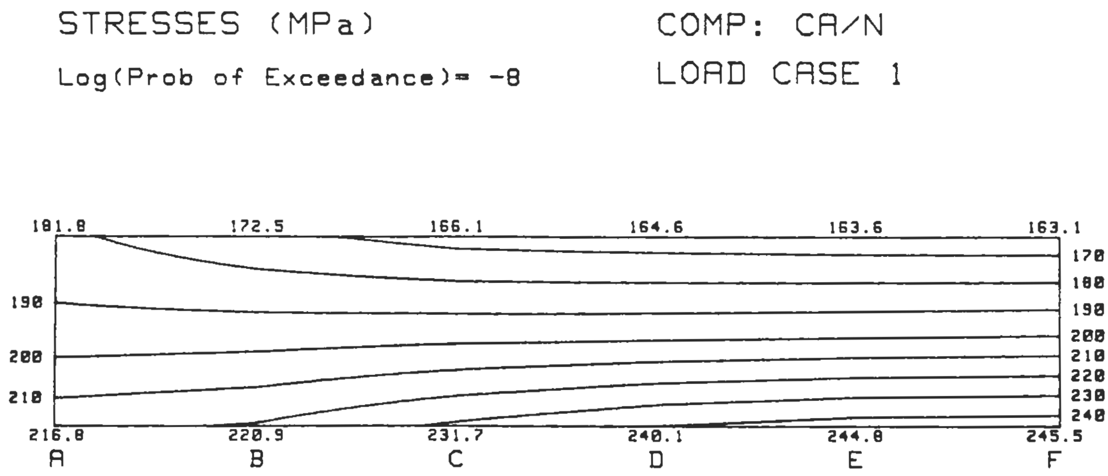
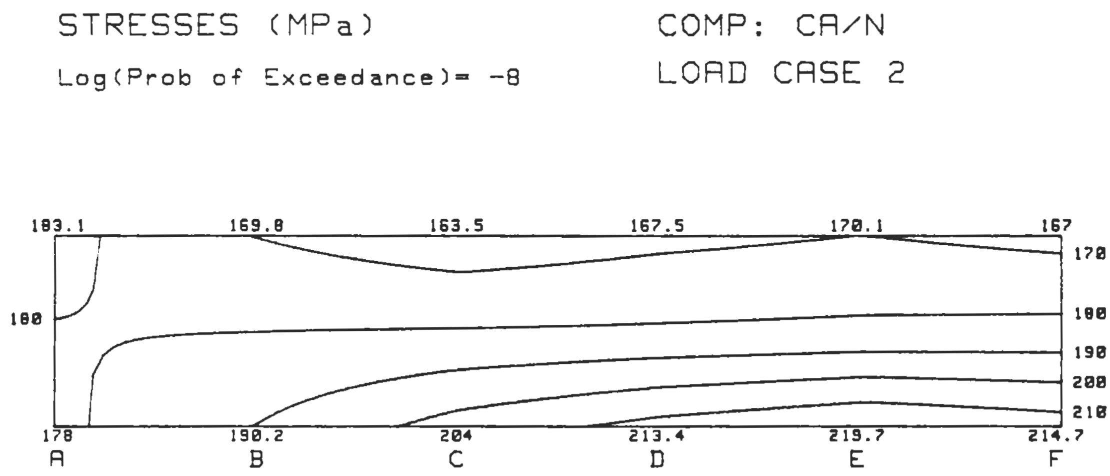


Fig.3.7 Distribution of most probable maximum long-term normal stresses in the bottom side girder. Homogeneous oil cargo condition. From ref.[2].





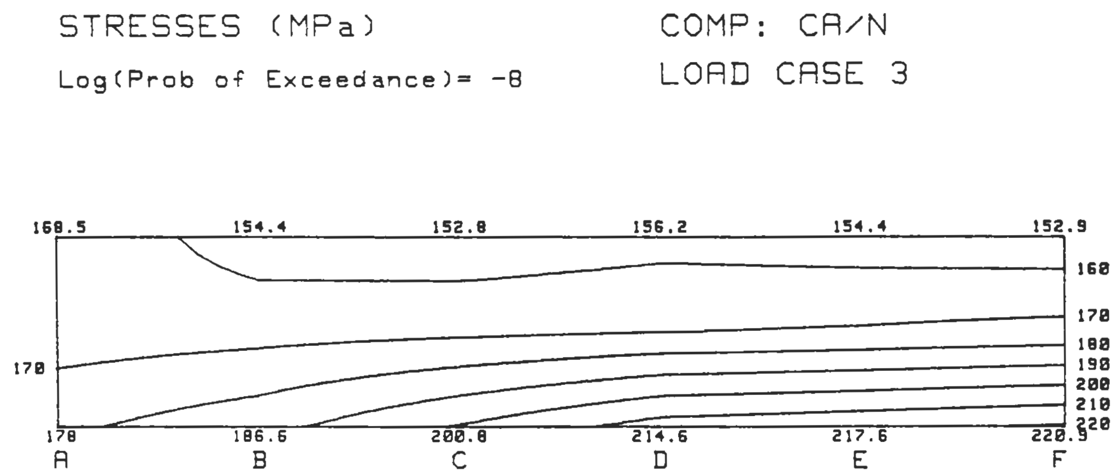


Fig.3.8 Detailed distributions of most probable maximum nominal combined long-term normal stresses in the bottom side girder, Hold 4. Wave statistics from North Atlantic Area 16. $n = 10^8$ stress cycles

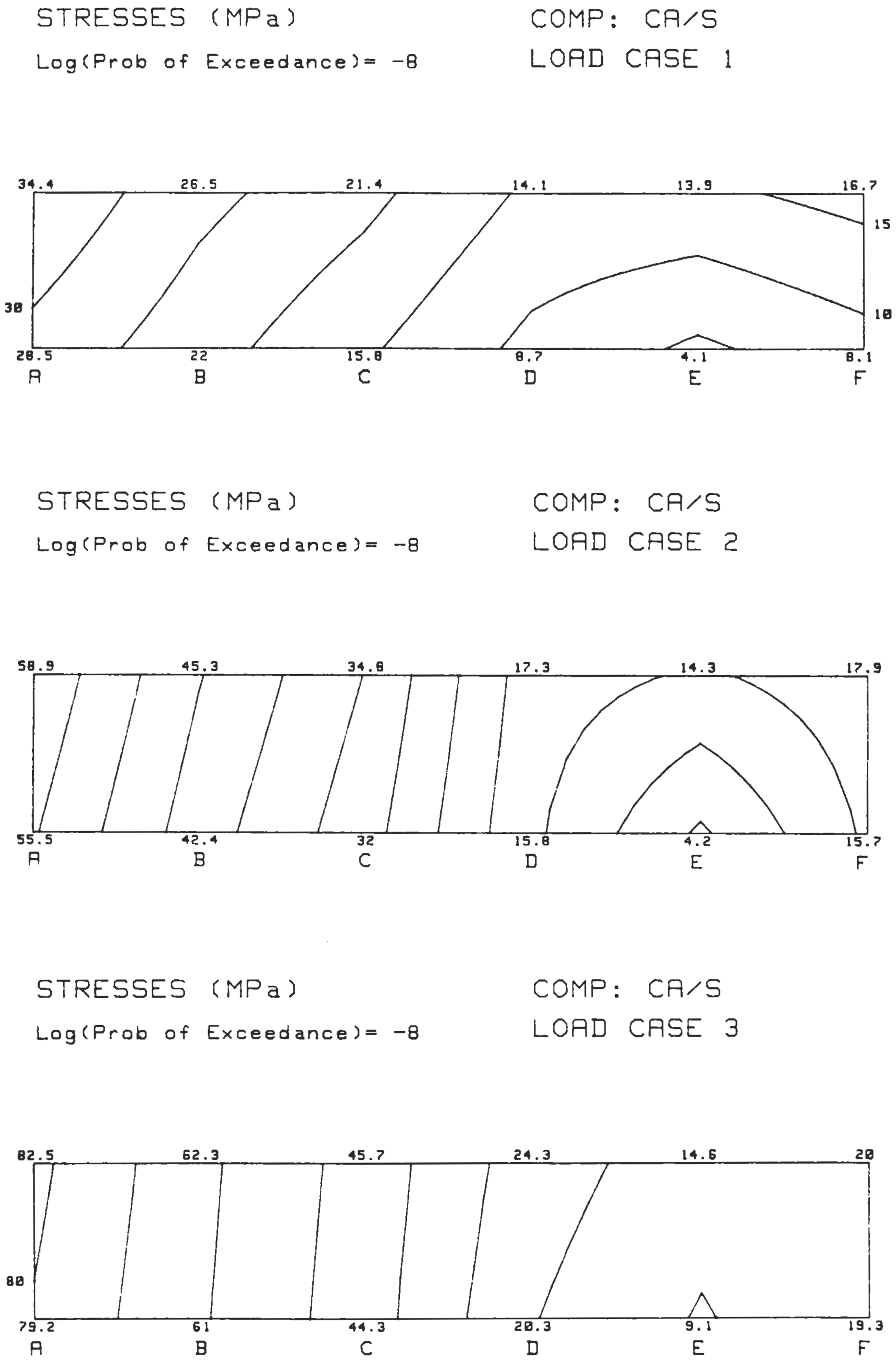


Fig.3.9 Detailed distributions of most probable maximum nominal combined long-term shear stresses in the bottom side girder, Hold 4. Wave statistics from North Atlantic Area 16. $n = 10^8$ stress cycles

Fig.3.10 below shows an example of the variation of the shape parameter h in the long-term Weibull distribution for normal stresses and oil cargo condition. Due to the interaction between global and local stress components, the shape parameter varies considerably with the vertical position in the girder close to the transverse bulkhead. The figure indicates that it is not sufficient to assume a general shape parameter in a simplified fatigue analysis.

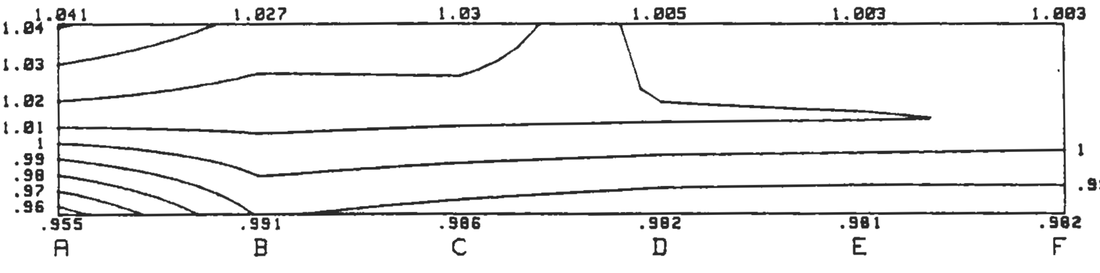


Fig.3.10 Detailed distribution of Weibull shape parameter h for nominal long-term normal stresses in the bottom side girder, Hold 4. Wave statistics from North Atlantic Area 16. Full load oil condition.

In order to illustrate the influence of different long-term wave statistics, the combined normal stress response at bottom in the middle of Hold 4 has been calculated for four other ocean areas in addition to the North Atlantic Area 16, see fig 3.12. The results are summarized in fig.3.11 below. It shows a large variation in long-term stress distribution for the different ocean areas, with the wave statistics from Area 16 giving the by far worst fatigue damage within the sample.

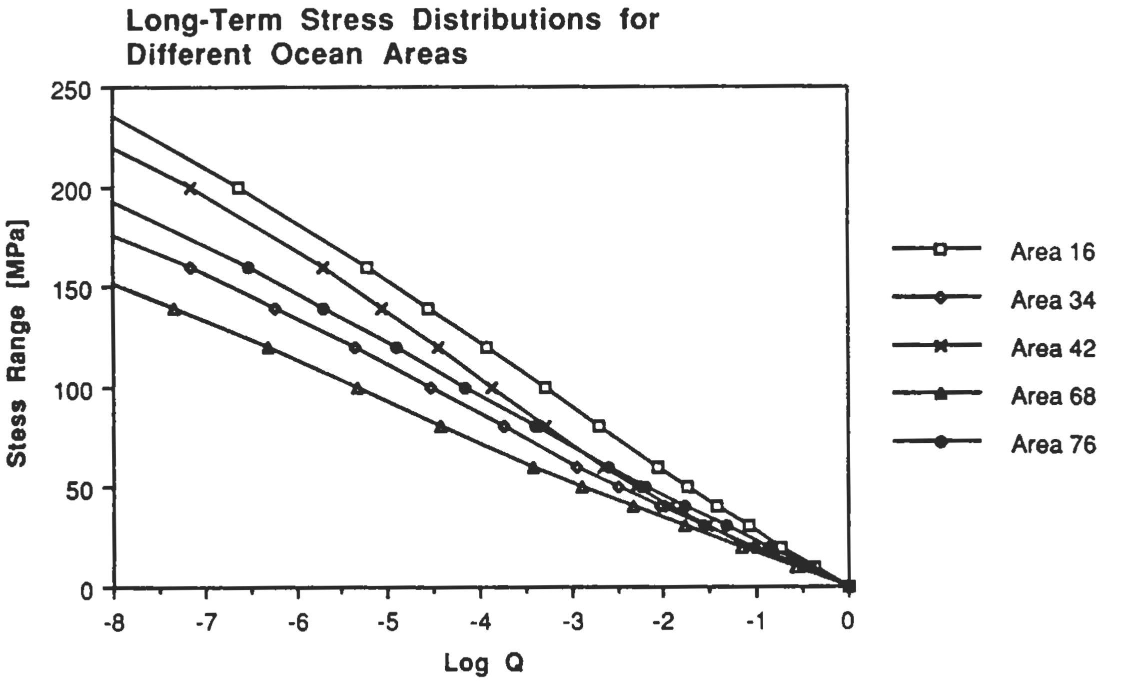


Fig.3.11 Comparison between long-term distributions in different ocean areas. The example shows total combined normal stress at bottom (section E)

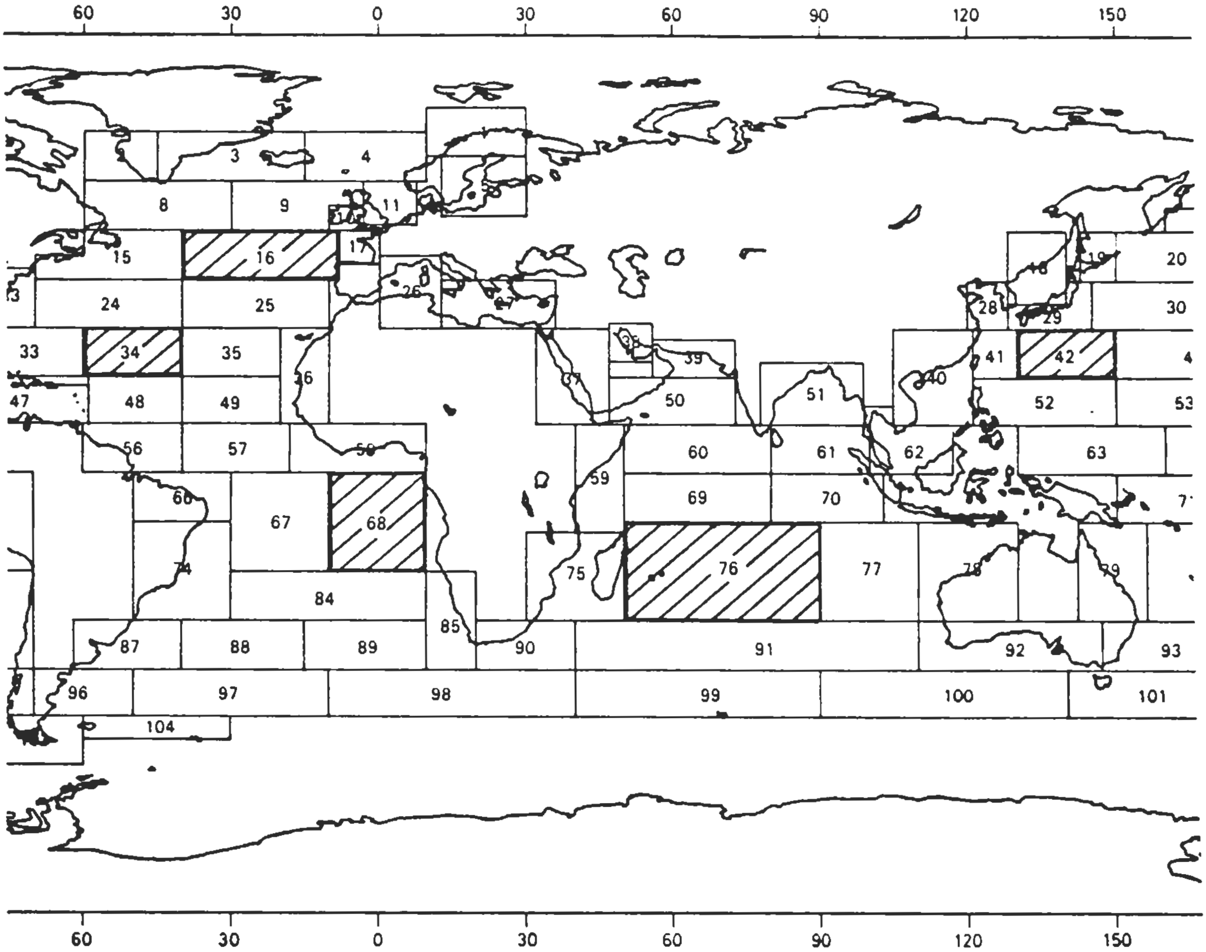


Fig.3.12 Ocean Areas for wave statistics, ref.[6]

3.3 *Fatigue analysis*

In order to show how to apply the results from long-term analysis of nominal stresses on actual weld joints and built-in stress concentrations, simplified fatigue analysis according to the Offshore Rules has been performed for the bottom girder "as built".

The cumulative damage ratio has been calculated for different hot-spots corresponding to different joint classes and stress concentrations as indicated in fig.3.13 and table 3.1. For the positions around the hole (positions 1-8) the actual correlation between nominal normal and shear stresses have been taken into account in the calculation of tangential edge stress.

Stress concentrations adjacent to the hole have been calculated with the fine mesh FE-model shown in fig.3.14. The model was loaded with prescribed displacements at the boundaries to simulate an even distribution of nominal normal and shear stresses, and actual edge stresses around the hole, and longitudinal stresses at the lower longitudinal stiffener (position 12) were evaluated. Fig.3.15 shows the distribution of largest principal stress σ_1 per unit nominal normal and shear stress respectively.
For the bottom edge with rat holes (position 11) structural stress concentrations are included in the SN-curves.

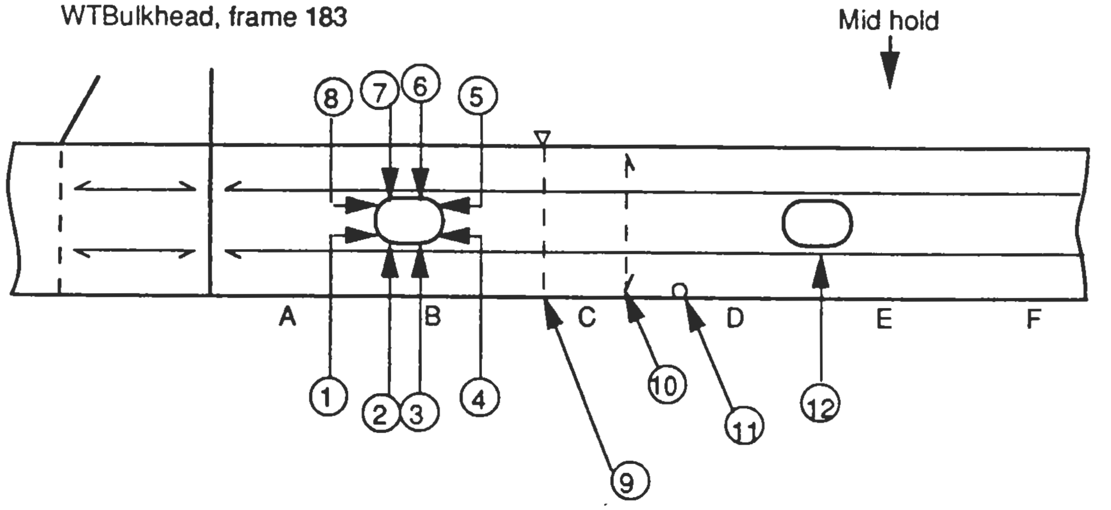


Fig.3.13 "Hot-spot" positions for fatigue analysis of the bottom girder as built

Description of hot-spot	Pos. No	SCF_σ	SCF_τ	Joint Class
Hole edge at max. stress concentration on nominal shear stress	1 and 5	1,0	-6,0	Class C
Hole edge at max. stress concentration on nominal normal stress	2 and 6	2,4	-3,7	Class C
Hole edge at max. stress concentration on nominal normal stress	3 and 7	2,4	3,7	Class C
Hole edge at max. stress concentration on nominal shear stress	4 and 8	1,0	6,0	Class C
Web plate butt weld at bottom	9	1,0	-	Class D
Web plate butt weld at hole (in reality only present in Hold 1,3,7)	3	2,4	3,7	Class D
Web plate butt weld at hole (in reality only present in Hold 1,3,7)	6	2,4	-3,7	Class D
Vertical stiffener ends at bottom	10	1,0	-	Class E
Bottom weld including rat holes	11	1,0	-	Class F
Lower long. stiffener adjacent to hole	12	1,6	-	Class C

Table 3.1 Data of analysed hot-spot positions

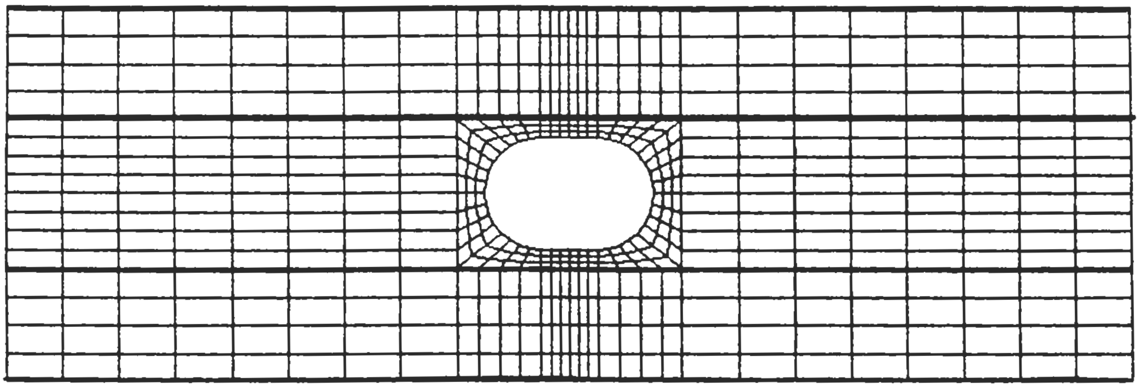


Fig.3.14 Fine mesh FE-model of bottom side girder

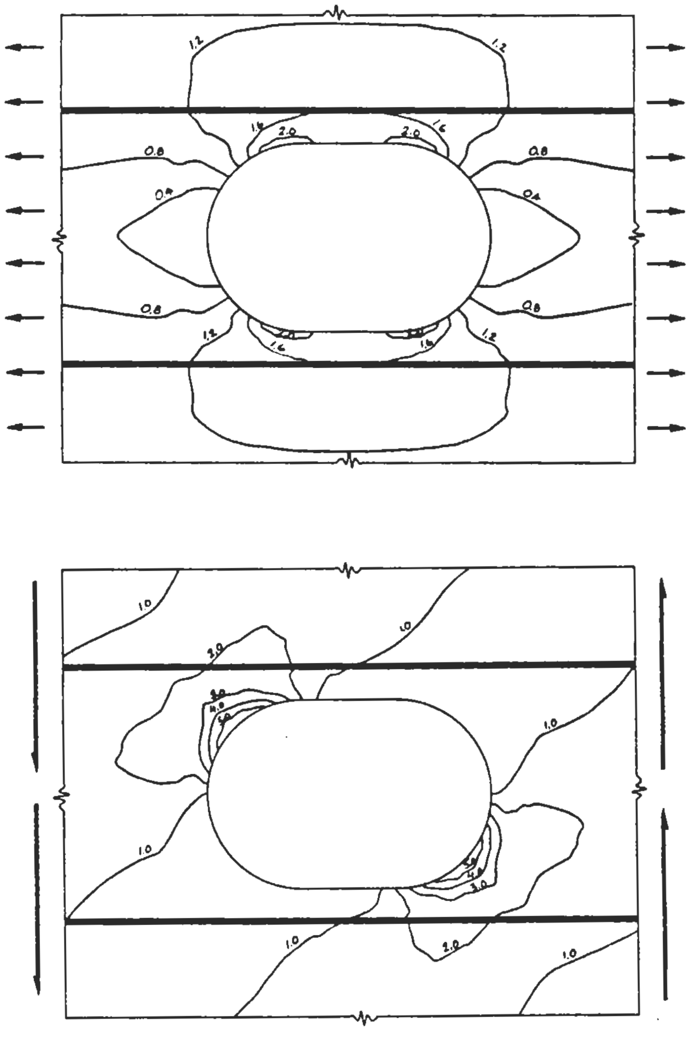


Fig.3.15 Iso-stress contours for principal stress σ_1 around the hole per unit nominal normal and shear stresses in the bottom girder

Figs.3.16-3.21 show results from fatigue analysis of the different positions. The damage ratio is calculated for SN design curves based $n = 10^8$ long-term stress cycles at 15 knots in the North Atlantic. Each load case is presented separately. Results for a combination of load cases can easily be achieved by adding together part-time damage ratios, one for each separate load case and corresponding total service time.

The figures show the longitudinal distribution of damage ratio with no regard to the actual as-built longitudinal locations of the hot-spots. This form of presentation has been chosen to show whether it is possible or not to find an optimum location of a detail e.g a manhole. The highest values of damage ratio are found at different positions around the hole edge. Due to the combined effect of nominal normal and shear stresses, the most critical position (2 in fig.3.13) shows a nearly constant value of damage ratio for different locations along the studied part of the girder. Hence no increased structural safety can be achieved by a better location of the hole, fig.3.16.

Fig.3.18 shows very high values of damage ratio if the web plate butt weld is positioned at the hole. According to the structural drawings, this position is present in Holds 1, 3 and 7, but not in the studied Hold 4. According to the previous fig.3.7 (and fig.6.8 in ref.[2]) the combined wave induced normal stresses are significantly lower in the adjacent Hold 3 (while the local shear stresses are of the same magnitude). However, it must be emphasized that a butt weld at the free edge of a hole with large geometric stress concentrations is a very bad design with respect to fatigue.

The SN design curves based on mean minus two standard deviations are associated with a low probability of fatigue damage. For stationary offshore structures this seems justified considering the difficulties to performing frequent structural surveys, and the catastrophe that might follow from a major structural damage. However, for ships in unrestricted service, special surveys are performed at 4-5 years intervals and discovered fatigue cracks will be repaired. If the mean SN-curves are used for the fatigue analysis, the corresponding damage ratios will be reduced by about 60% in comparison with damage ratios calculated from SN design curves. Table 3.2 below shows approximate "design life" and "mean time to failure" for the studied as built hot-spot positions. "Failure" is here to be interpreted as a visible fatigue crack which, because of structural redundancy, not necessarily will lead to a major structural failure.

Description of hot-spot	Oil DL/MTF [years]	Bulk DL/MTF [years]	Ballast DL/MTF [years]
Hole edge position 2, section C	6 / 16	8 / 22	7 / 19
Girder web butt weld joint at bottom position 9, section F	23 / 61	30 / 79	24 / 63
Vertical stiffener ends at bottom position 10, section F	16 / 50	21 / 65	16 / 52
Bottom weld at rat hole position 11, section F	10 / 26	12 / 34	10 / 27
Lower long. stiffener adjacent to hole position 12, section F	23 / 59	31 / 80	26 / 67

Table 3.2 Design life (DL) and mean time to fatigue failure (MTF) for different hot-spot positions and different load conditions in the bottom girder, Hold 4. Wave statistics from North Atlantic Area 16. Service speed 15 kn.

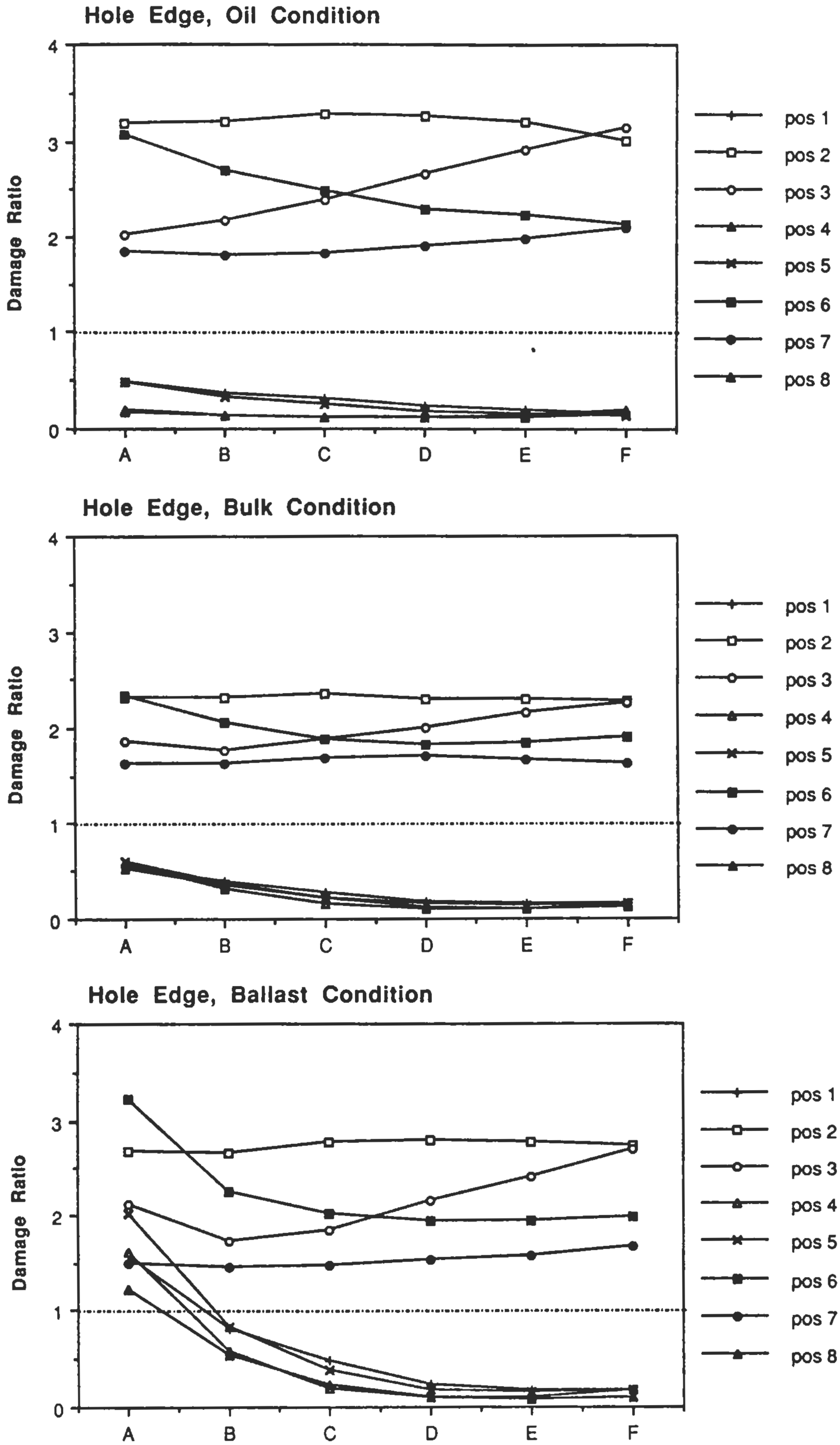


Fig.3.16 Cumulative damage ratios around the hole edge, $n = 10^8$ stress cycles North Atlantic wave statistics, SN design curves for class C

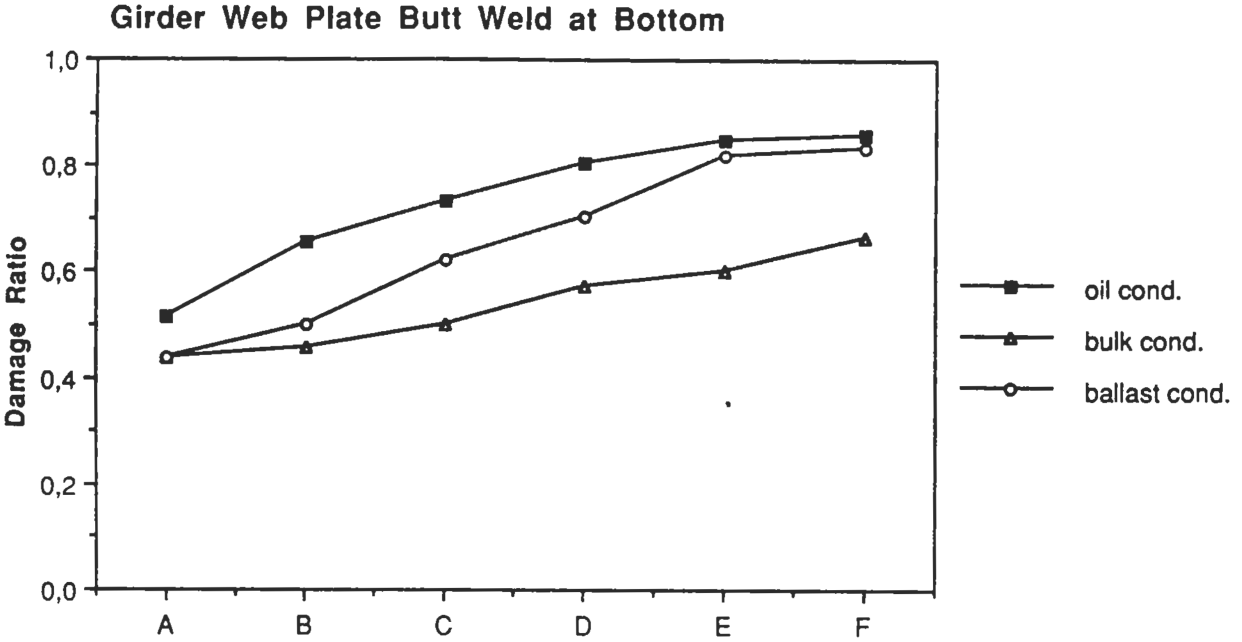


Fig.3.17 Cumulative damage ratios for web plate butt weld, $n = 10^8$ stress cycles North Atlantic wave statistics, SN design curves for class D

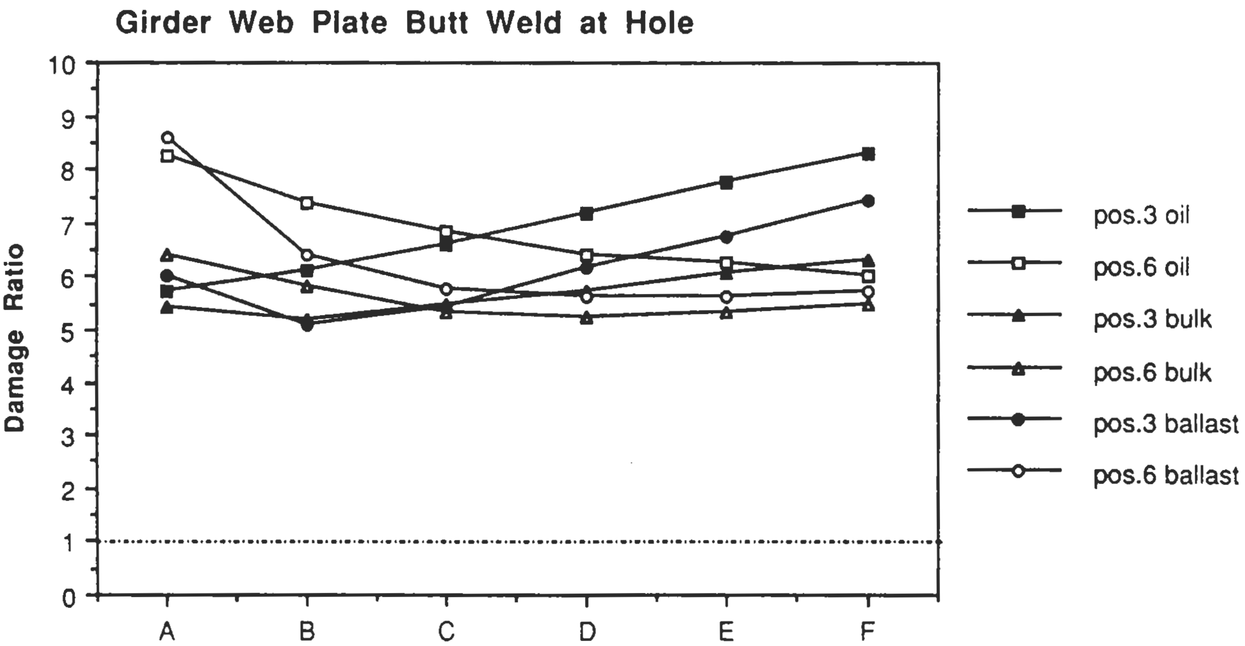


Fig.3.18 Cumulative damage ratios for a butt weld at the hole, $n = 10^8$ stress cycles. North Atlantic wave statistics, SN design curves for class D

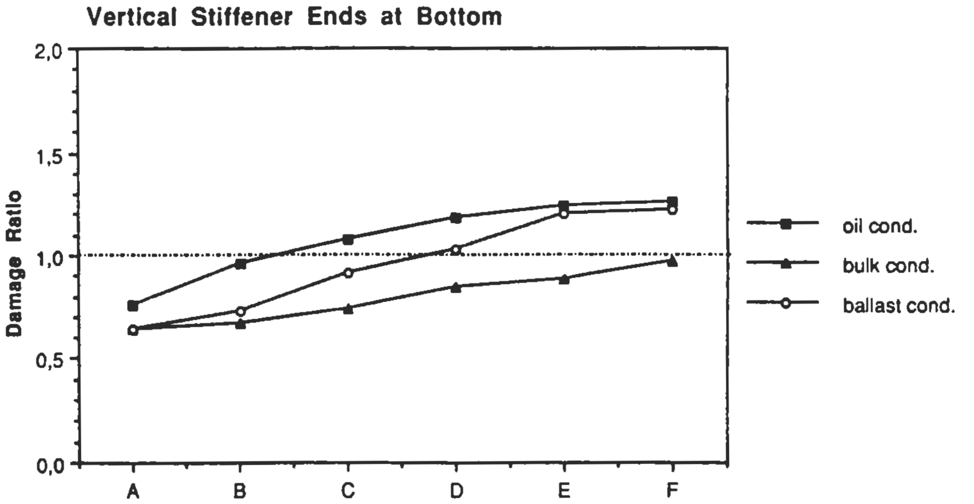


Fig.3.19 Cumulative damage ratios for vertical stiffener ends, $n = 10^8$ stress cycles North Atlantic wave statistics, SN design curves for class E

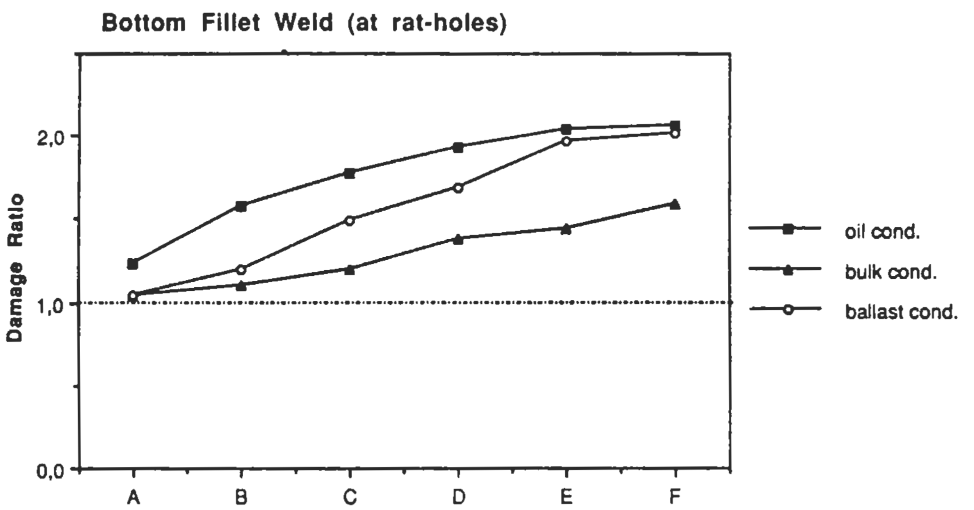


Fig.3.20 Cumulative damage ratios for bottom weld, $n = 10^8$ stress cycles North Atlantic wave statistics, SN design curves for class F

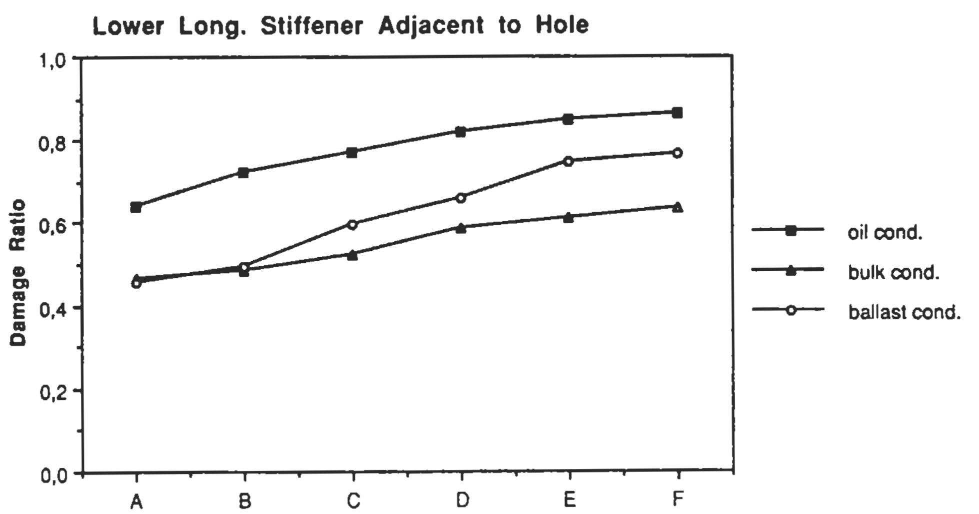


Fig.3.21 Cumulative damage ratios for long. stiffener, $n = 10^8$ stress cycles North Atlantic wave statistics, SN design curves for class C

4 CONCLUSIONS

Detailed distributions of combined wave induced long-term nominal stresses in the bottom side girder of an OBO carrier have been calculated with a direct method. The highest longitudinal normal stress levels are found to occur in the middle of Hold 4 amidships for an evenly distributed full load oil cargo condition. The results based on North Atlantic wave statistics show significantly larger extreme stress levels than those given by the design loads in the Classification Rules.

Fatigue strength analysis based on the long-term stress distributions have been performed with a simplified method according to the rules for mobile offshore units. The largest risk of fatigue damage in the bottom side girder is found at the free edge of a manhole in the girder web. The most critical position is where the stress concentration for longitudinal normal stresses is highest. Due to the combined effect of nominal normal and shear stresses, the calculated fatigue life is, in practical terms, constant over the studied part of the hold. The second largest risk of fatigue damage was found at the fillet weld between girder web and bottom shell, adjacent to a rat-hole in the middle of the hold. The calculated cumulative fatigue damage ratio is here based on nominal normal long-term stress distribution, and the local geometric stress concentrations are included in the SN design curve for joint class F. If the actual local stress concentration for both normal and shear stresses was taken into account, the damage ratio would probably be larger and more evenly distributed over the girder length.

The quantitative values of predicted fatigue damage at the different hot-spot positions are based on a simplified analysis incorporating uncertainties and systematic errors, most of which are on the conservative side:

* The representation of the long-term stress distribution by one single Weibull-distribution for all probabilities of exceedance as used here in the simplified fatigue analysis according to eq.(2.16) and fig.2.2, tends to overestimate the fatigue damage with 10%-20%, ref.[2]. If the fatigue limit S_0 is taken into account, the damage ratio will be further reduced 5%-10% depending on the damage ratio level.

* Results from the fatigue analysis above are based on assumption of a constant service speed of 15kn in the North Atlantic Sea. If average world wide sea state statistics is used, the fatigue life will be significantly increased. The influence of speed reduction and weather routing is much more significant on the extreme stress than on fatigue. Based on the simplified study of the influence of speed reduction reported in ref.[2], a reduction of the damage ratio by only a few percent is estimated.

* The long-term probability of exceedance calculated according to eq.(2.11) does not include the influence of bandwidth or mean periods of the short-term stress response spectra. Both these quantities can be included in a more refined statistical analysis. This should also preferable include a better statistical representation of short-term sea spectra, such as the family of six-parameter wave spectra suggested by Ochi, [9].

However, the purpose of this study is not to establish the best possible method for prediction of the probability of fatigue damages, but rather to show the usefulness of direct calculations of long-term combined wave induced stresses for design purposes. The results show that even with a simplified analysis, detailed qualitative conclusions can be drawn about the risk of fatigue damages for different hot-spots and weld joints in the structural members.

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