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Ultimate Hull Girder Strength Margins in Present Class Requirements

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ABSTRACT

The objective of the paper is to give an indication of the actual ultimate hull girder strength margins inherent in present ship design practise as laid down in the rules of the major classification Societies. For the sake of simplicity Det norske Veritas' Rules are used as a reference.

Failure modes that control the ultimate strength of a ship hull are discussed. A brief review of the development leading up to to-days advanced nonlinear finite element procedures is given and some common pitfalls in the use of such methods are commented on.

A nonlinear superelement approach, FENCOL, used within the framework of the general nonlinear shell program FENRIS is outlined. The paper compares numerical strength predictions from FENCOL with the test results of a large scale box girder and a VLCC that broke her back in harbour during unloading operations. Results of analyses of another 3 tankers and one all hatch cargo vessel are presented. Both large 3-D models as well as cross-section models have been used.

The predicted strength margins are commented on with a view to inherent safety levels and the development of future reliability based ship design rules for ultimate strength control.

INTRODUCTION

Assessment of the ultimate longitudinal strength of a ship's hull under loads imposed by the sea has traditionally been made by comparing calculated elastic stresses in the deck or bottom shell members with allowable stresses, usually corresponding to prescribed fractions of the material yield strength. The actual fractions to be used are now normally determined by the strength format in the design code used and appropriate strength checks (buckling, yield). This approach, applied in conjunction with a nominal estimate of vertical wave bending moment, is valid for conventional ships resembling previous successful hulls designed in the same way, but may fail in providing a true estimate of overall hull ultimate strength. For unconventional designs more sophisticated methods (as outlined herein) may have to be used in combination with direct wave load calculations (strip theory and/or sink-source methods). If the hull structure does not fail locally, the actual collapse bending moment may exceed the moment which nominally causes outer-fibre yield; on the other hand the collapse moment may be substantially less than the nominal yield moment if local compressive failure of plating or stiffened panels occurs in parts of the cross-section.

However, the early day procedures did not cater for the extensive redistribution of stresses (redundancy) in the hull girder following collapse of local areas. This is in particular the case for double hull constructions where considerable potentials for redistribution exist by way of longitudinal material and transverse elements as girders and bulkheads.

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Element Failure Modes

The ultimate strength of a ship depends on the combined strength of the various structural components. The governing failure modes are:

1. Compression buckling including
   - post-buckling instability and
   - post-collapse residual strength
2. Tension-tearing rupture
3. Brittle fracture

All elements in the compression flange of a ship do not normally fail in compression at the same overall load level. Therefore, a comparatively accurate description of the post-collapse curve of these elements is essential for both the ultimate capacity and post-collapse behaviour of the hull.

Ductile tension-tearing due to immediate excessive overloading may be initiated by fatigue cracks and/or production defects. Larger cracks will behave in a more brittle manner. The net effect is a reduced pre-rupture tension capacity (i.e. critical rupture strain) as compared to a "perfect" material. Brittle fracture will normally not be a problem due to use of ductile steels and general good workmanship.

Tension failure in new ships are normally not a problem provided defects are kept within accepted production tolerances, but may become a concern as ships get older and run down.

In any case, both compression and tension capacities have to be kept in mind in ultimate strength analysis procedures.

Review of Ultimate Strength Procedures

In 1965 Caldwell presented his paper entitled "Ultimate Longitudinal Strength", ref. [1]. The paper presented a "component" approach for calculating the ultimate longitudinal strength of a ship from the scantlings and material properties of its cross-section. Starting with the simple case where failure occurs through material yielding only, the method was extended to allow for the effect of buckling of the compressed parts of the structure.

Even if all necessary information on the strength of the various structural elements of the cross-section was not available at that point in time, the paper provided a working reference that stimulated subsequent work in the field.

Ultimate Strength Design of warships is common practise for reasons of weight efficiency. Hence, it was only natural that the ideas were further developed for and adapted to naval vessel design. In the UK the Admiralty Research Establishment (ARE) played a leading role, ref. nos. [2-3]. Their method considers whipping response to impulsive loads in addition to quasistatic loads. The strength formulation as such is based on approximate characterisation of the strength of elements in the hull cross-sections under tensile and compressive loads associated with hull-girder bending together with local lateral pressure effects. The influence of imperfections (initial deformations and residual stresses) is accounted for. The theoretical results were correlated with experimental data derived from collapse tests on a number of stiffened box-girders.

A computer program along the same lines were also developed by Lloyd's Register in 1979, [4]. The program is based on the ARE approach for vertical bending and also includes horisontal bending about the vertical axis.

A parallel development based on the component approach also took place in the U.S. and again closely related to naval vessel design, ref. [5-6]. However, common for the developments referenced so far is the lack of attention to tension rupture effects. The American Bureau of Shipping (ABS) has developmed the USAS program together with Columbia University, ref. [8-10]. The mesh fineness has been simplified by introducing inelastic orthotropic elements enabling the modelling of larger areas as one element. Still it appears necessary to maintain a few nodes at free spans in order to account for initial geometric imperfections. However, it may not be satisfactory to assume that local buckling and loss of stiffness of plate elements can be ignored, even where stiffeners are closely spaced. Significant loss of stiffness can result from premature yield caused by residual stresses and by local lateral buckling as well as torsional buckling. In rectangular plates with b/t as low as 30, buckling and consequent loss of stiffness may arise if compressive stresses occur in the shorter direction. The presence of shear stresses will have a marked degrading effect as well.

The best way around this difficulty appears to be modification of the orthotropic constitutive equations using effective stress-strain curves for rectangular plate elements under uniaxial stress, or combined direct stresses and shear stress. Such curves, accounting for local buckling and imperfection effects, have been established by extensive parametric studies, ref. [31]. This route has been followed by several institutions such as ARE [2-3], LR [4] and DnV [11], [15], [18] and [35].

The analysis in ref. [8] takes into account elastoplastic properties of the material, nonlinear geometric behaviour of the elements, and their buckling and post-buckling strength. Selected applications to typical ship structure problems are shown. Tension rupture effects are not accounted for.

In ref. [9] a discussion of assumptions and modelling principles is given along with more examples of applications of the method. The modelling fineness shown assumes a node to be placed at the points of maximum deflections of the buckling or collapse modes. This is natural since this approach represents the coarsest mesh possible for producing meaningful results. However, with this way of modelling, and the deficiencies pointed out above, the ultimate strength predictions will appear to be on the optimistic side. Some further considerations have been reported in [10] adapting the results to a reliability safety format.

The safety considerations in ref. [11] are based on further developments of the method used in [15]. Here, stress-strain curves for uniform biaxial direct stress, shear stress and lateral pressure are used for establishing orthotropic constitutive relations for rectangular plate elements. Tension rupture effects are included by the introduction of a critical rupture strain derived from the CTOD design curve [16]. The computer program used is a special version of the general purpose nonlinear program FENRIS named FENCOL (Finite Element Nonlinear COLlapse/COLlision). "Component approach" type of models can be set up as well as full 3-D nonlinear models capable of identifying the structural redundancy and progressive collapse behaviour.

Ref. [11] gives examples of ultimate strength analyses of a bulk carrier double bottom and an oil tanker. In the latter case a part of the midship area spanning over 4 frames and one transverse bulkhead has been modelled. Both crack and corrosion sensitivity studies were performed. This exercise is further discussed in the following. The ship is referred to as case vessel no. 3.

Comparison between predictions of ultimate hull girder strength and experienced full scale accidents in the past have been attempted. The failure of two Japanese destroyers in 1935 has been examined in ref. [12], and a more recent container ship failure (1977) in ref. [13]. In both cases nonlinear bow flare effects were found to yield a significant increase in longitudinal wave sagging moment, which were not fully accounted for in design. The loading values were compared to ultimate hull girder strength predictions with methods as outlined above.

A similar attempt was untertaken by the ARE in the UK. Here, a destroyer was tested to failure [2] and comparisons with numerical strength predictions were made. These showed a reasonable agreement accounting for the fact that the loading block arrangement used introduced high shear stresses in the ships sides in way of the supports. This was not accounted for in the numerical strength predictions.

Two ship hull box girders have been tested to ultimate failure at University of California, Berkeley [14]. Analytical and experimental strength predictions are discussed in depth along with possible reasons for the discrepancies experienced. Test no. 1 has been used in this paper for comparing with solutions obtained with the FENCOL program, Fig. 4.

Both LR [41] and DnV have compared their programs to the collapse of the VLCC "Energy Concentration" that failed in port during unloading operations, ref. [40]. The DnV comparisons are being discussed in this paper.

Impact of Modelling Fineness on Strength Predictions

It has been argued that as soon as a new generation of super computers become available, there will be no more need for simplified ultimate strength prediction methods as those outlined above. At such point in time standard general purpose nonlinear Finite Element Codes (e.g. ASAS-NL, ADINA, STAGS, FENRIS) for ultimate strength analyses can be used sucsessfully for the same purpose without undue penalities on computer time.

Examples of such analyses on surface vessels as well as submarines are given in ref. [7]. This, however, needs a very experienced (expert) user with a keen understanding of nonlinear modelling techniques; effects of imperfections, etc. and preferably with a fair amount of testing experience as well. The possibilities for faulty modelling is considerably larger than with linear FEM analyses.

Hence, the practicality of such an approach is still rather questionable, and will remain to stay that way for the foreseeable future. This is illustrated in the following with a collapse strength prediction study of an unstiffened plate using FENRIS with two different meshes.

A (2 x 2) and a (10 x 10) element mesh with four-noded quadrilateral thin shell elements

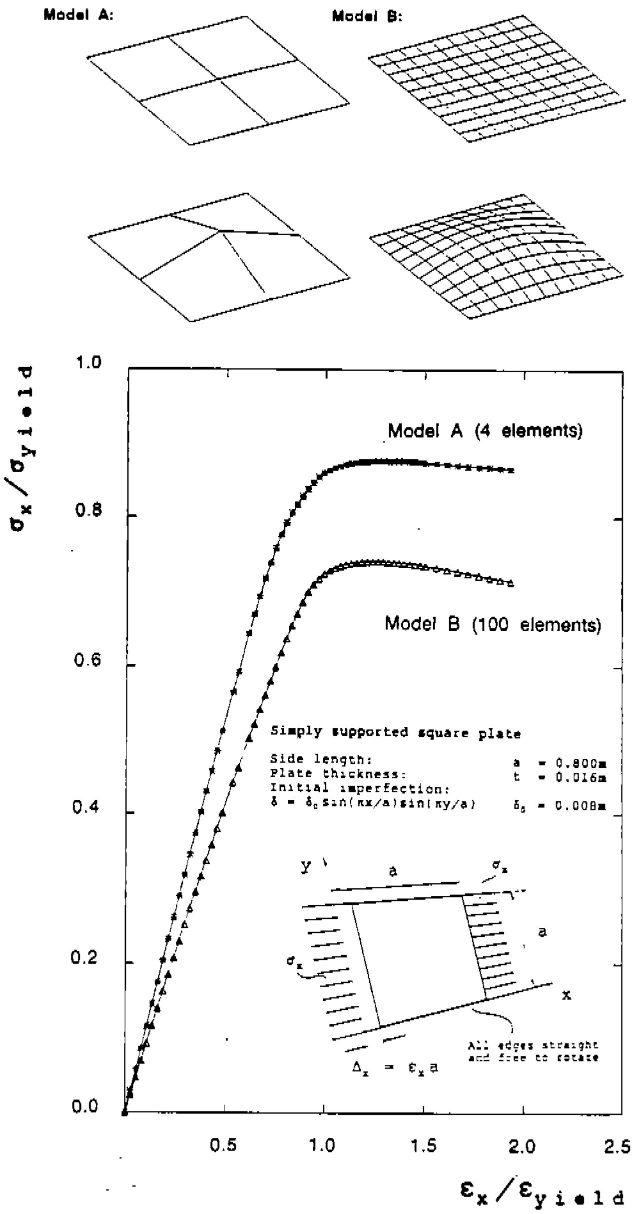


Fig. 1 Effect of Mesh Fineness on Plate Capacity Predictions

with 5 integration points through the thickness were used. Fig. 1 shows the results, the associated element meshes and the lateral deformation modes at their respective uniaxial in-plane load levels at collapse. The coarse model behaves both stronger and stiffer as compared to the finer model with about 20% and 30% respectively.

This simple example highlights the dangers of relying on "conventional" non-linear FEM strength predictions of complex structures. Without using a very fine mesh and accounting for production defects (e.g.out-of-straightness and to some extent residual welding stresses) both strength and stiffness may easily be overpredicted.

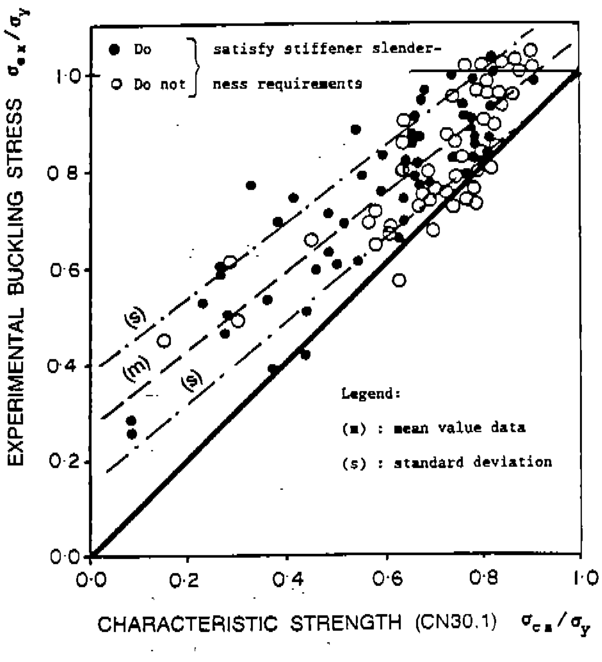


Fig. 2 Model Uncertainty - Stiffened Panel in uniaxial Compression

ELEMENT STRENGTH FORMULATIONS

Examples of load characteristics used in FENCOL are shown in Fig. 3b. Such curves are relevant for uniaxial load situations. For combined load situations, bi-axial compression/tension, shear and lateral pressure, interaction curves have to be applied, [23] or [24].

Elements in compression

Recommendations as to the strength of single elements in the hull, e.g. stiffened plates, girders e.t.c., are in general given in design codes. Compression failure (buckling - failure mode 1) is covered in e.g. ref.[19-20]. Useful information can also be found in [23-24]. A comprehensive overview and comparison with test results is presented in [21]. The present version of FENCOL applies buckling strength criteria from CN30.1, ref. [20] and a generalisation of the interaction formulas in ref. [23].

The component strength prediction given by design codes are usually defined as characteristic values. This means that there is a defined small probability (confidence limit) that the actual strength is less than the given value. In modern design codes the confidence limit is usually chosen to be in the order of 5%.

Fig. 2 is taken from ref. [21] and shows the model uncertainty inherent in the uniaxial panel buckling strength formulation used in FENCOL. The model uncertainty can be approximated by a mean value and a standard deviation as follows:

Mean value (m):

$$E[\sigma_u/\sigma_y] = 0.795[\sigma_u/\sigma_y] + 0.270 \qquad (1)$$

Standard deviation (s):

$$S[\sigma_u/\sigma_y] = 0.116 - 0.034[\sigma_u/\sigma_y] \qquad (2)$$

These formulas may serve as an estimate in the range

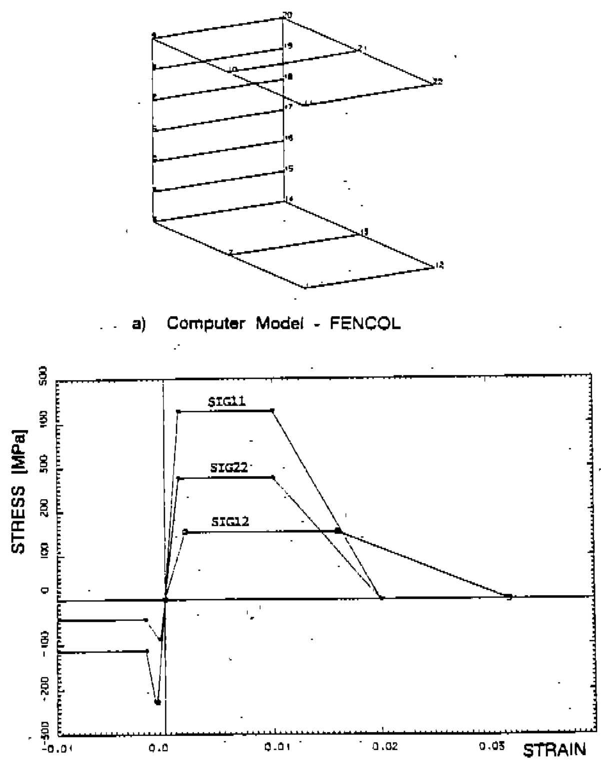
$$0.2 < \sigma_u/\sigma_y < 0.95$$

Information on post-collapse strength of stiffened plate panels is rather limited. However, some information is available from DnV [25-26] and Cambridge University [27]. These are both test results as well as numerical analyses and have been systemised and put to use in the load characteristics used in FENCOL.

Some information on the subsequent crushing load level for uni-axial compression is also available, ref. [28-29].

Elements in tension

Information on brittle fracture (failure mode 3) can be found in textbooks on fracture mechanics, e.g. ref. [22].



a) Computer Model - FENCOL

b) Material Curves for Deck Panels

Fig. 3 Computer Model - Berkeley Box Girder Test no.1

However, more important for the ultimate strength of a ship hull girder is the tension tearing rupture mechanism (failure mode 2). When a defect is of a size such that the critical rupture strain is in the order of the yield strain a tension rupture in the tension flange of the hull girder will be equally damaging for the ultimate hull girder strength as a buckling failure in the compression flange.

Tension-tearing can be described with a Crack Tip Opening Displacement (CTOD) criterion [22]. For normal production tolerances (intact ship) the critical strains will be very large. Ref. [16] lists strains in the order of 5 - 10 % depending on material quality, yield strength, temperature, plate thickness and strain rate at rupture. However, critical strain levels of this order of magnitude are far too high to constitute any problem for ultimate hull girder strength evaluations.

More important in this context is the question; - how large is the critical through-thickness crack that brings the critical rupture strain down in the order of the yield strain? A similar CTOD analysis yields that such cracks will be of the order of 50 mm and above. This indicates that in primary structural members only through-thickness cracks with a length of 50 mm and above are of any significance for the immediate ultimate strength of the hull girder.

VERIFICATION OF COMPUTER PROGRAM FENCOL

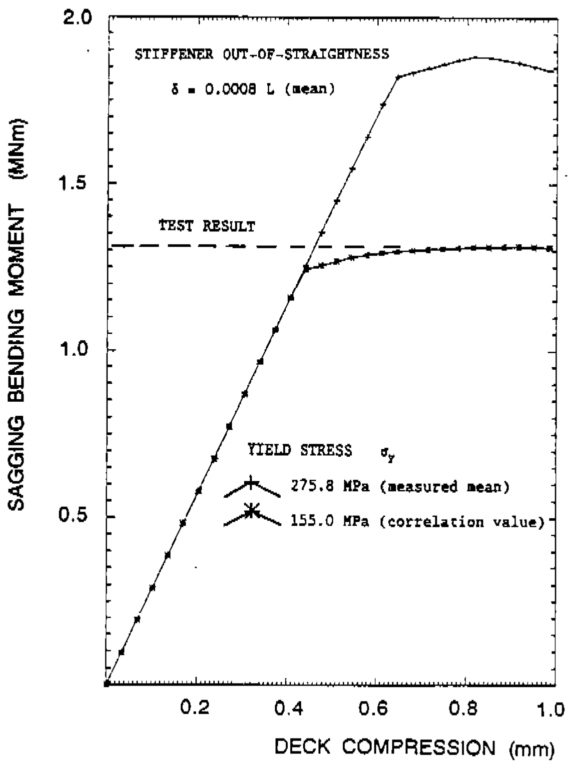
As outlined previously several methods have been used for Ultimate Strength Analysis of ship hull girders. These methods are mostly based on a combination of experimental, analytical and/or numerical determination of stiffened panel strengths together with a more or less sophisticated integration of these characteristics over the cross-section.

However, in the end the final validation of such procedures has to be based on large-scale model testing, or even better, on full-scale experience. The procedure used in DnV has on the element strength level been correlated to test data on plate strength, Fig. 2. System level correlation studies are reported in the following.

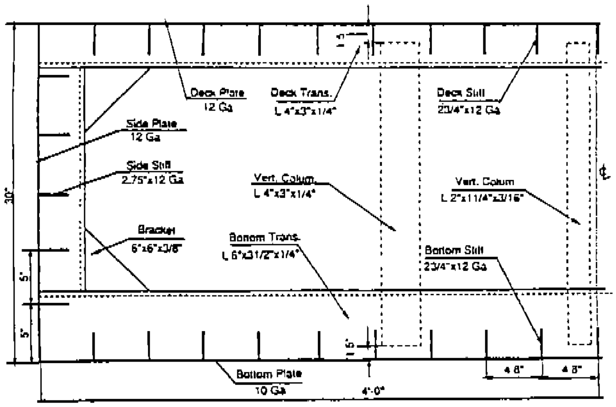
Large Scale Box Girder Test

Two large box girders representative for actual ship structures have been tested at University of California, Berkeley [14]. Test no. 1 has been used here as one of the correlation cases for the computer program FENCOL. The box girder cross-section is shown in Fig. 4b. The corresponding computer model and the material curves for the deck panels are shown in Fig. 3. The model was tested in sagging. Prior to testing the model was measured for imperfections (lateral out-of-straightness of plates and stiffeners) and strain gages were mounted in several bays in the cross-section.

The measured imperfections had a rather wavy



a) Ultimate Strength Comparisons



b) Box Girder Cross-Section

Fig. 4 Box Girder Strength Analyses

form with a maximum deviation towards the stiffener side in the order of 0.6% of the length of the stiffeners (frame spacing). This points in the direction of a plate induced (PI) failure which in fact did not happen. The failure was reported to be torsional/flexural buckling of the longitudinals, i.e. stiffener induced (SI) collapse. This is also supported by the fact that the single panel strength in the (SI) mode is only 63% of that in the (PI) mode. Further, the stiffener slenderness is quite high - the height to thickness ratio is h/t=25. This is about twice as much as what would have been allowed according to DnV practise in order to avoid lateral/torsional stiffener buckling [20].

The standard stiffener imperfection value used in FENCOL is the standard DnV offshore rule value, i.e. one sinusiodal half-wave with a maximum deviation of 0.0015 L where L is the frame spacing (stiffener length). The initial deviation that will trigger a stiffener induced failure is a lateral impefection towards the plate side and/or a sideways (horisontal) deformation (tilt) of the stiffeners.

When comparing test results with analyses the most probable outcome of a test is the 50% "fractile", i.e. the mean value. This means that mean value data for strength parameters as well as analytical strength formulations should be used. Hence, the yield stress was taken as σ_y = 275.8 MPa (40.000 psi) and the lateral stiffener out-of-straightness as the mean value of measured production deviation in full scale structures; $\delta/L = 0.0008$. The mean value panel strength defined by eq.(1) and Fig. 2 was used. For this case Fig. 4a shows a substantial overprediction of the strength, approx. 44 %, relative to the test result.

As a next attempt the effect of sideways tilt of the stiffeners was explored. A tilt different from a sinusiodal half-wave with a maximum deviation of 0.0015 L over the stiffener length is not explicitely taken into the strength formulation used in ref. [20]. The effect of a possible larger tilt was investigated by specifying a lower yield stress for the stiffener.

The reduced stiffener yield stress that matched the test result was found to be 155 MPa, or 56 % of the actual material yield stress. This corresponds to a characteristic torsional stiffener strength σ_k = 143 MPa in the strength formulation. Then, the strength of the flat-bar stiffener was derived analytically as that of a plate with simple support at the hull plate, at transverse web frames and having a free upper edge and a sideways tilt corresponding to one sinusiodal half wave over the length of the stiffener. A strength of this plate equivalent to that of the reduced yield stress was found to correspond to an equivalent tilt amplitude of $\delta/t=3.5$ or $\delta/L= 0.016$. This means that the 2.78 mm thick flat-bar stiffener might have had a 9.73 mm sideways tilt amplitude. With a stiffener height of h=69 mm this means that $\delta/h=0.14$.

The above values does not seem unrealistic for a model scale construction that by virtue of the relatively higher welding heat input will tend to have higher welding distortions than in full scale.

Full Scale Correlation - VLCC

The collapse of the VLCC Energy Concentration in Europort on July 22 1980 has been used as the full scale correlation case. The report from the official inquiry after the accident is available in ref. [40]. The ship collapsed in hogging due to incorrect operation during cargo discharge in harbour. The actual still water load at collapse is listed as 17,940 MNm or 1,829,000 tm. The vessel was built in Japan in 1970 and was reported to be in good condition with very limited corrosion at the time of the accident.

MOMENT CAPACITIES

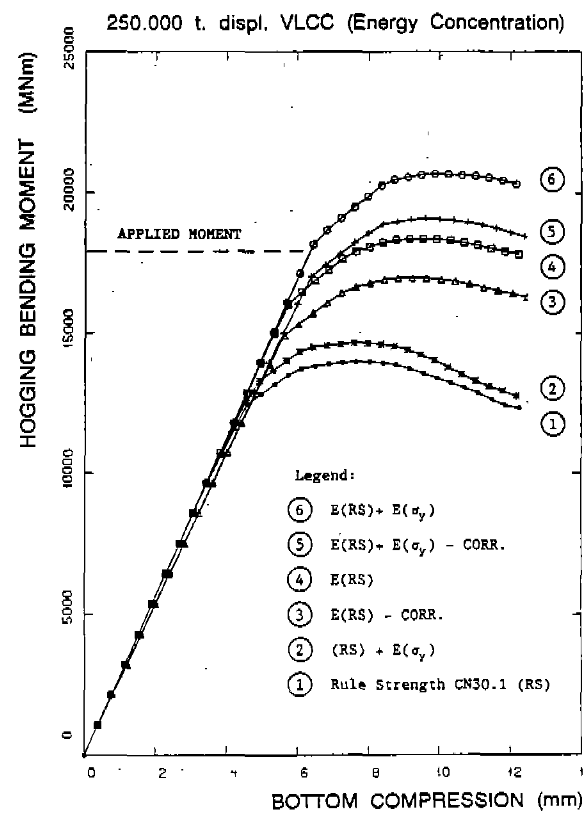


Fig. 5 Full Scale Verification Case

The cross-section Finite Element Model for the vessel used in FENCOL is shown in Fig. 7a. The moment capacity curves in Fig 5 are fairly linear up to the point where a major collapse of the bottom panels takes place. However, the lower part of the centreline bottom girder collapses at a much lower load level. This is associated with the collapse of a comparatively small area, and contributes as such insignificantly to the strength and stiffness of the overall cross-section. Hence, the governing failure mechanism is the collapse of the bottom panels. The subsequent strength reserve comes from redistribution of the load in the cross-section following further collapse of panels in sides and longitudinal bulkheads.

Depending of the assumptions made in the calculations altogether 8 different simulations were made of which 6 are shown in Fig. 5. Table 1 outlines the various cases w.r.t. strength formulation, yield stress, imperfection level and corrosion assumption. The strength predictions varies from +15% to -22% of the actual collapse moment.

TABLE 1 **Strength Predictions of Ship no. 2 - Energy Concentration**

Case no.	Strength model DnV CN30.1	Yield σ_y [MPa]	Imperf. level δ/L	Corrosion	Mom.Cap. M_U [MNm]	Panel σ_u [MPa]	Pan.Mom Cap.M_{CR} [MNm]	Cross-sect margin $B_C=M_U/M_{CR}$
6	E(RS)	400	0.0008	no	20670	263.3	18170	1.138
5	E(RS)	400	0.0008	1 mm	19103	256.8	16934	1.128
4	E(RS)	315	0.0008	no	18363	232.9	16072	1.143
4A	E(RS)	315	0.0015	no	17987	229.7	15851	1.135
3	E(RS)	315	0.0008	1 mm	16978	227.5	14993	1.132
2	(RS)	400	0.0008	no	14649	191.7	13229	1.107
1A	(RS)	315	0.0008	no	14424	186.1	12843	1.123
1	(RS)	315	0.0015	no	13984	182.5	12594	1.110
Section modulus: W = 69.009 m³ (intact) 65.903 m³ (corroded)								(m) : 1.127 (s) : 0.013 COV : 0.011

Legend: E(RS) means the expectance (mean) value of the basic CN30.1 panel Strength formulation, ref. [20]

The case closest to the test result of 17.940 MNm is no. 4 which is 2.4% above the actual value. Here, mean value panel strengths from eq. (1), mean value stiffener imperfection, but nominal yield stress were used.

A uniform 1 mm corrosion loss of plate thickness was assumed in case 3 and 5. From experience this is a reasonable value for uncoated tanks, and is for reasons of comparison the same value as used in a recent Lloyds study, ref.[41]. The strength reductions listed in Table 1 are in the order of 7.5% as compared to Lloyds' results of 6%.

Previous studies, in-house in DnVC and elsewhere, on the variability of yield strength of plate material delivered from Japanese steel mills indicates a mean yield stress for NV-32 steel (σ_y=315 MPa) of at least 400 MPa with a coefficient of variation COV=0.066, see e.g. ref. [42]. This also adds to the uncertainty of which solution should be used as the "correct" one.

There is regrettably no easy answer to this question. However, the present comparisons with numerical analyses shows they are in the right range with reasonably close estimates when using mean value strength formulations. Otherwise, for evaluation of strength margins as well as overall safety margins, reliability based procedures need to be used.

Hull Girder Strength Predictions

The collapse moment derived from the strength of the panel in the compression flange that actually governs the onset of final collapse of a ship's cross-section, M_{CR}, is defined as the panel strength (σ_u) times the section modulus (W) to the panel neutral axis. This corresponds to the knuckle point in the moment capacity curves in Fig. 5. However, as seen from the results there are strength reserves in the cross-section beyond the onset of first panel collapse. This is listed in Table 1 and 3-5 as

Cross-section Margins, defined as

$$B_C = M_U/M_{CR} \qquad (3)$$

where M_U is the Ultimate Collapse moment.

Hence, the Ultimate Moment Capacity can be written as:

$$M_U = B_C \; \sigma_u \; W \qquad (4)$$

The cross-section strength margin B_C will vary with the actual build-up of the cross-section. This means that at least one nonlinear Ultimate Strength prediction will have to be carried out in order to determine a basic margin B_{C0} specific for the particular cross-section. After having done so, the results in Table 1 indicate that B_C can be approximated with a linear function of σ_u/σ_y, i.e.

$$B_C \simeq 0.123 \; \Delta[\sigma_u/\sigma_y] + B_{C0} \qquad (5)$$

where $\Delta[\sigma_u/\sigma_y]$ is the variation in σ_u/σ_y from the reference value at which B_{C0} has been calculated. In this way the effects of strength formulation, yield stress, imperfection level and corrosion effects is approximately

TABLE 2 Principal Dimensions of Case Vessels

Case Vessel no.	L_{PP} [m]	B [m]	D [m]	d [m]	C_B	L/D	Tonnes displ. [TD]	Dead-weight [TDW]
1 Box	12.802	2.438	0.762	-	-	16.8	-	-
2	313.0	48.2	25.2	19.69	0.821	12.42	250,000	217,500
3	275.9	44.2	22.43	17.15	0.8252	12.3	177,100	155,200
4	194.2	32.0	16.0	10.0	0.799	12.14	50,900	-
5	232.6	38.1	16.6	12.6	0.805	14.01	92,100	-
6	150.0	22.93	13.25	9.73	0.560	11.32	19,200	-

accounted for by their influence on the controlling panel strength. In case of corrosion, the corroded hull girder section modulus will have to be used in eq. (4) above.

It should be noted that eq. (5) is derived based on the full numerical results for one vessel cross-section only and should as such be used with some caution.

An attempt to extract a COV on B_c for use in reliability analyses is listed in Table 1. By calculating the mean value and associated standard deviation from the 8 numerical cases given, it appears that a COV in the order of 1.5-2% may be used in connection with predictions given by eq. (5) above.

SCANTLINGS OF CASE VESSELS

The principal dimensions of the case vessels are listed in Table 2. Altogether five different vessels and one large scale box girder test section have been considered:

The box girder test designated "Case vessel no. 1 Box" in the Table was designed to simulate a single skin tanker construction of 75,600 TDW.

Vessel (2),(3) and (5) are conventional single skin constructions wereas vessel (4) and (6) have double bottoms.

Ship (2) is the verification case outlined above, see Fig. 5 and Fig. 7a. This vessel is one in a series of at least nine vessels built in Japan in the early 70'ies. The hull section modulus at the bottom panel stiffeners was 69.009 m^3. She was built with a section modulus margin in bottom equal to 10% on the minimum unified section modulus requirement.

Ship (3) is one of a successful series of vessels from a European yard built in the mid 70'ies and has section moduli margins relative to the minimum unified section modulus requirement in deck and bottom equal to 5.1% and 12.5% respectively. High tensile steel (NV-36, σ_y=355 MPa) was used in deck and bottom areas.

Ship (4) has a double bottom and double side construction, Fig. 7c, and was initially built with section modulus margins in deck and bottom corresponding to 12.8% and 19.5% respectively. Prior to going into service the vessel was strengthened to fully comply with offshore requirements for 100 year design loads rather than 20 years. Note that all characteristics of the vessel quoted herein refer to the initial unmodified condition.

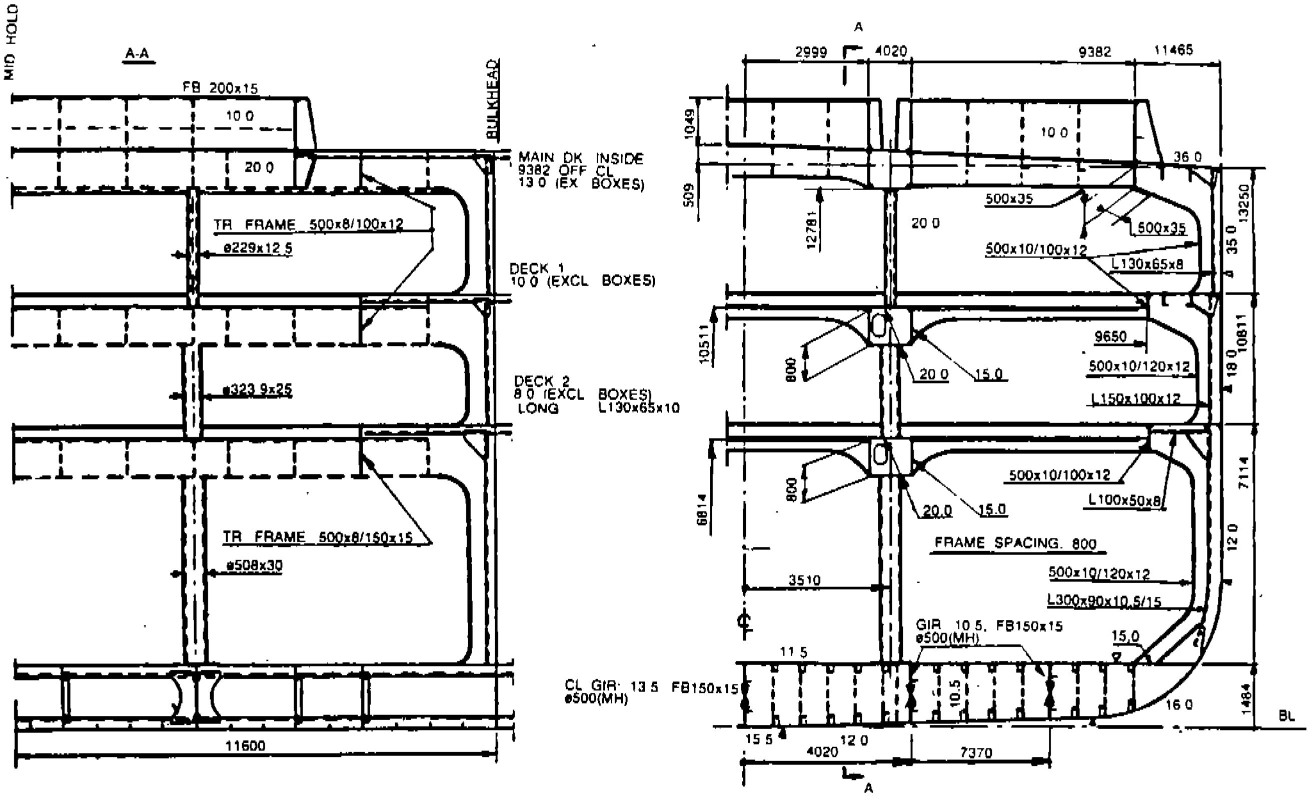
Vessel (5) and (6) have been dimensioned according to DnV Rules for Classification of Steel Ships [32] based on principal dimensions, still-water moments and arrangements as specified by USCG. The resulting midship sections are given in Fig. 6. These ships were part of a DnV in-house Ultimate Strength study [35]. The Ultimate strengths of the same vessels, but with ABS scantlings, were reported in ref. no. [9].

The Handy-Size Tanker (5) was designed and optimized towards IACS unified longitudinal strength requirements on minimum section modulus as allowed by the major Classification Societies. Thus, this vessel has no margin on section moduli in deck and bottom.

The all hatch cargo vessel (6) was dimensioned to hogging and sagging still-water moments considerably higher than standard DnVC rule values. In order to assess the efficiency of the longitudinal box girders a separate FEM analysis was performed. The analysis demonstrated a 70% efficiency of these parts.

According to standard Classification Society practise actual section moduli are calculated with corrosion margins included. Buckling control of strength members, however, is based on net cross section without corrosion margins (ship rules).

When using High Tensile (HT) steel DnV ship rules gives the most credit in terms of steel weight saving in hull girder material when



PRINCIPAL DIMENSIONS, ALL HATCH CARGO VESSEL

RULE LENGTH	150.000m	LONGITUDINAL SPACING	670
BREADTH MLD	22.930m	BOTTOM LONG.	L300x90x11 5/16
DEPTH MLD:	13.250m	INNER BTM LONG	L250x90x11.5/16
DRAUGHT	9.729m	MAIN DK LONG	FB 360x36
BILGE RADIUS	3.000m	MAIN DK LONG	
CAMBER	0.509m	BETW HATCHES	
BLOCK COEFF	0.56	AND DECK 1 LONG	L130x65x10

Fig. 6b All Hatch Cargo Vessel (6)

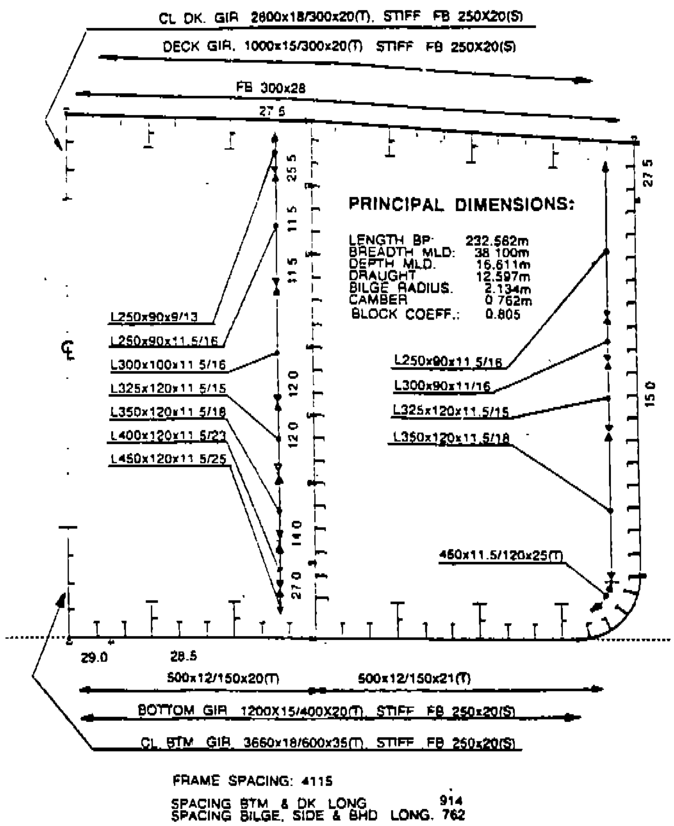


Fig. 6a Handy-Size Tanker (5)

designing with a length to depth ratio (L/D) in the order of 12.0-12.5. During the 70'ies most tankers were designed in this way, Table 2.
In this range the midship section is determined by the longitudinal hull girder section modulus requirement which is directly proportional to the inverse of the material factor f_1 (for NV-36, f_1=1.39). For larger L/D ratios the moment of inertia requirement gradually governs the design. This relates to hull girder deflection, and natural hull girder frequencies and is independent of whether HT steel is used or not. On the other hand, L/D ratios lower than 12, e.g. in the range 10 to 12 result in a midship section determined by local lateral pressure type criteria to plates and stiffeners. This effect is increasing towards the lower L/D range and is approx. a function of $1/\sqrt{f_1}$ showing that the potential weight saving in using HT steel here is not as marked as in the L/D=12.0-12.5 range.

COMPUTER MODELS

Half the midship section (symmetry with respect to the centre line plane) has been modelled for

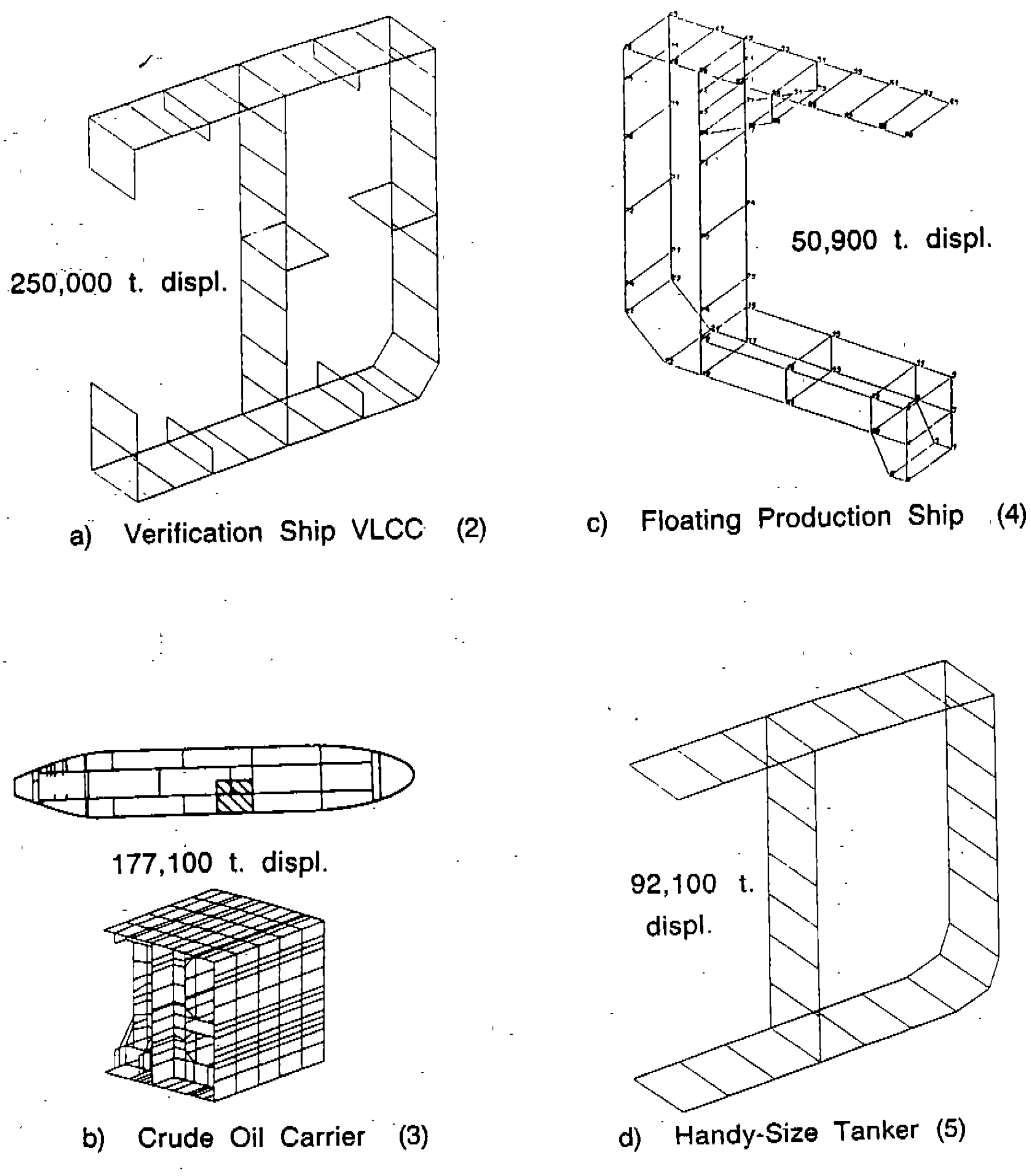


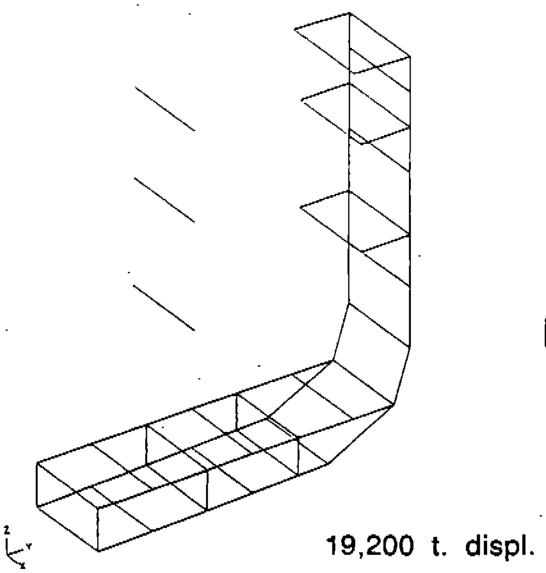
Fig. 7 Computer Models of Case Ships

all the vessels. The element meshes are shown in Fig. 7-8. The one bay models are restricted to cover the section between two transverse frames. With the modelling technique used in FENCOL it is sufficient to model only one element in the longitudinal direction to describe the combined longitudinal strength of the different structural members in the cross section.

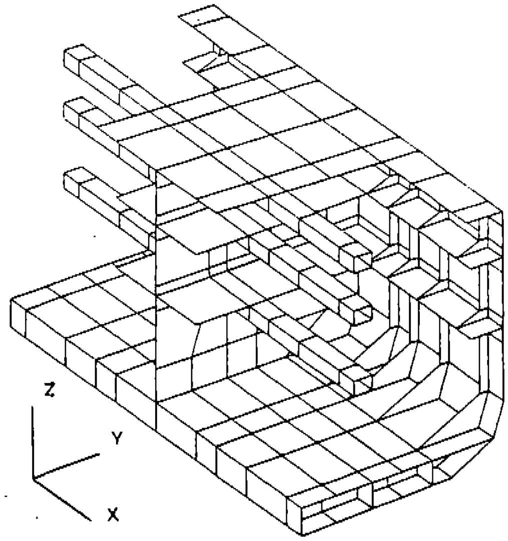
Three different types of elements are used in all three models; two noded bar elements, four noded quadrilateral membrane elements and two noded beam elements.

Girders or flanges of girders or webs are modelled by use of one bar element and a corresponding stress/strain relationship describing the complete structural behaviour in tension and compression. Stiffened plate panels are modelled by use of several quadrilateral membrane elements each with corresponding stress/strain relations (biaxial compression-/tension and shear).

One end of each model is fixed to induce symmetry about the midship plane, the other is attached to a stiff beam frame on which the sagging or hogging bending moment is applied.



a) **Small Model**



b) **Large Model**

Fig. 8 Computer Models of All Hatch Cargo Vessel (6)

The stiff frame ensures that the loaded end of each model remains plane during deformation.

The crude oil carrier (3) was modelled with a fairly large 3D model spanning from and including a transverse bulkhead in the midship section to mid of the side tank aft of the bulkhead, Fig. 7b. From an Ultimate Strength prediction point of view the use of a large model was not strictly necessary. However, the model was used to be able to consider the effect on ultimate strength from cracks in transverse strength members, ref. [11],[18].

For the all hatch cargo vessel (6) both a small and a large model was made. The purpose of the two models was to investigate the amount of additional strength reserves (redundancy) that can be quantified with each type of model and in particular in double skin constructions. The large model spans from the centre of one hold to the centre to the next hold including the transverse bulkhead. Both models are shown in Fig. 8. The box girders at the hatch openings are modelled with membrane elements instead of bar elements as was the case for the small model of the all hatch cargo vessel. On one end of the model (the midship plane) symmetry is introduced and on the other the sagging and hogging moments are applied over a stiff beam frame.

When using both one-frame models and larger 3D models great care is exercised with boundary conditions and the way the hull girder loads are applied to ensure that the effective neutral axis shifts correctly as the collapse progresses. This is vitally important in order to arrive at a correct solution.

STRENGTH PREDICTIONS AND COMPARISONS WITH RULE VALUES

The hogging or sagging bending moment is applied to each ship hull incrementally (step by step) until the maximum load carrying capacity is reached. After this point the ship hull collapses causing the bending moment capacity to decrease as the deformation continues, see Fig. 5. At ultimate capacity a major part of the hull has collapsed and no more strength reserves can be obtained by redistribution of stresses.

The FENCOL overall strength predictions are based on "as-built" dimensions (including corrosion margins) and includes the effects of geometric imperfections (e.g. stiffener out-of-straightess) and residual welding stresses.

The numerical strength predictions for case vessels (3) through (6) are based on the basic rule strength formulations (RS) from ref. [20]. The resuls are summarized and compared to standard ship rule design practise in Table 3 and 4. The correlation case (2) results are shown in Table 1. Mean value strength estimates based on the procedure given in eq. nos. (1) through (5) above are listed in Table 5.

In the design moments (M_D) listed rule values or actual approved and/or specified still water moments have been applied together with the 20 year wave bending moment specified in DnV ship rules [32]. The ship rule capacity moment ($M_{CR} = \sigma_a W$) corresponds to the allowed utilization of the nominal rule buckling strength of

TABLE 3 Comparisons of predicted Ship Hull Girder Ultimate Strengths with current Ship Design Practise

Case Vessel no.	2	3	4
Type: Size:Tonnes displ.	VLCC 250.000	Crude Oil Carrier 177.100	FPS 50.900
Load Cond.	hogging	sagging	sagging
Ultimate Str. Pred. FENCOL M_U Small Model [MNm] Large Model	 13984(RS) -	 - 11996(RS)	 - 3646(RS)
Yield stress (compr. flange)	315	355	315
DnV Ship Rules: - Cr.buck. σ_c [MPa] - $\sigma_a=\sigma_c\cdot\eta^1$(allow) - Section Modulus - Ship rule design load moment M_D - Single component Rule Capacity M_{CR}	 219.3 197.4 69.009 13142 13622	 291.1 262.0 41.000 9610 (8880)² 10742	 226.5 203.9 15.146 3131 3088
- Rule Capacity Margin M_{CR}/M_D - Cross-Section Margin $B_C=M_U/M_{CR}$ - Design Capacity Margin M_U/M_D	 1.037 1.027 1.064	 1.118 (1.210) 1.117 1.248 (1.351)	 0.986 1.181 1.164
Location of Failure (rule)	Bottom panel longitudinal	Dk. longitudinal center tank	Dk. longitudinal center tank

¹ η = 0.9 torsional stiffener buckling/plates in double bottom
= 0.85 lateral stiffener buckling
= 1.0 single plate buckling

² Values in parantheses are based on direct load calculations

the strength member in the compression flange (i.e.deck/bottom) that actually controls the onset of overall collapse of the hull girder.

The listed rule capacities relative to the design load levels (M_{CR}/M_D) are measures of the nominal strength reserves in the ship hull girders as determined by conventional design practise. Further, the cross-section margins ($B_C=M_U/M_{CR}$) in Tables nos. 3-5 indicate the additional overall strengths of the vessels beyond what can be estimated from a single governing panel in the compression flange. This is a measure of the inherent level of redundancy in the hull girder. Table 5 lists predictions based on ref. [20].

Finally, the design capacity margin (M_U/M_D) compare the overall bending moment capacities with the design load values and is as such a measure of the ULS safety (strength) level built into the hull girder. Table 7 summarises the margins based on dirct rule strength predictions (RS) and estimated mean value strengths (m).

<u>Large v. Small Scale Models</u>

Due to its ability to redistribute stresses by way of longitudinal and transverse strength members a double skin/grillage type construction exhibits a higher redundancy than a single skin construction. In general a larger ultimate strength margin would be expected for a double bottom construction than a single skin construction. Previous studies of a double bottom reported in ref. nos. [11] and [18] indicate that double bottom constructions may be quite optimised. The inherent cross-section margins B_C will depend on size of vessel, length to depth ratio (L/D) and local structural arrangement (stiffeners/plates) of the compression panels controlling the collapse.

The overall strength prediction of the all hatch cargo vessel is as expected higher with the large 3D model (in hogging) than with the smaller cross-section one as this model in addition to resistribution of stresses in longitudinal material also includes stress redistributions in transverse elements. The

TABLE 4 **Comparisons of predicted Ship Hull Girder Ultimate Strengths with current Ship Design Practise (min. DnV rule scantling designs)**

Case Vessel no.	5		6	
Type	Handy-Size Tanker		All Hatch Cargo Vessel	
Size Tonnes displ.	92.100		19.200	
Load Cond.	sagging	hogging	sagging	hogging
Ultimate Str. Pred. FENCOL M_U Small Model	5583(RS)	5531(RS)	1673(RS)	1426(RS)
[MNm] Large Model	-	-	1617(RS)	1482(RS)
Yield stress (compr. flange)	235	235	235	235
DnV Ship Rules: - Cr.buck. σ_c [MPa]	220.5	202.8	229.9	165.8
- $\sigma_a=\sigma_c\cdot\eta^1$(allow)	187.4	182.5	195.4	149.2
- Section Modulus	29.130	31.814	7.241	8.871
- Ship rule design load moment M_D	5418	5530	1229	1170
- Single component Rule Capacity M_{CR}	5459	5806	1415	1324
- Rule Capacity Margin M_{CR}/M_D	1.008	1.050	1.151	1.132
- Cross-Section Margin $B_c=M_U/M_{CR}$	1.023	0.953	1.143	1.119
- Design Capacity Margin M_U/M_D	1.030	1.000	1.316	1.267
Location of Failure (rule)	Dk. panel long.(lat.)	Btm. panel long.(tors.)	Main Dk.pan. long.(lat.)	Outer Btm. plating

[1] η = 0.9 torsional stiffener buckling/plates in double bottom
= 0.85 lateral stiffener buckling
= 1.0 single plate buckling

reason why the large model does not produce the same trend in sagging is due to reduced stiffness and capacity associated with overall instability of the hatch coaming box girders. This is difficult to model properly in the small model which in this case will tend to produce too optimistic results. Consequently, in both sagging and hogging the large model results represent the best estimates.

Table 4 shows that the deviation between the large and small model for the All Hatch Vessel (6) in hogging is amounting to approx. 4%. The possibilities for redistribution of loads (load shedding) by way of transverse web frames and longitudinal stringers in a double bottom box/ grillage system after failure of local areas is quite substantial (see Fig. 6 in ref.[18]) and larger than in a single skin construction. The Cross Section Margins $B_C=M_U/M_{CR}$ in Table 5 amount to 1.084 and 1.246 for deck and bottom respectively. Hence, the redundancy margin of the double bottom construction is significant.

DISCUSSIONS - SAFETY LEVELS AND FUTURE RULE TREND

The results presented in this paper list design capacity margins (M_U/M_D) for present designs in the range from 1.00 to 1.32 for basic rule strength predictions (RS) and from 1.22 to 1.62 for mean value estimates (m), Table 7.

With modern weight optimised designs, e.g. vessel (5), the margins appear to be squeezed towards a bare minimum. Hence, it may be quite natural to question how low the margins can be reduced without impairing overall safety levels. This is clearly also a question of economy, i.e. to find the optimum balance between construction cost and maintenance costs such as to minimise the total life cycle costs while at the same time maintaining the necessary minimum safety standard. These issues were adressed in ref. nos. [11] and [18] indicating that if the necessary cash flow was available it would be economically beneficial to increase the safety standard.

TABLE 5 Bending Moment Capacities and Cross-section Strength Margins

Vessel no.	Panel Strength CN30.1 σ_u [MPa]			Panel Moment M_{CR} [MNm]		Cross-section margin		Ultimate Moment Cap. M_U [MNm]		
	(RS)	(m)	(s)	(RS)	(m)	B_{co}	B_c	(RS)	(m)	(m)/(RS)
2 hog	182.5	230.2	30.3	12594	15886	1.110	1.129	13984	17929	1.282
3 sag	241.2	287.6	33.0	9889	11792	1.213	1.229	11996	14493	1.208
4 sag	210.9	252.7	29.4	3194	3827	1.142	1.158	3646	4433	1.216
5 sag	168.4	197.3	21.5	4905	5747	1.138	1.153	5583	6627	1.187
5 hog	135.4	171.1	22.7	4308	5443	1.284	1.303	5531	7092	1.282
6 sag	208.1	228.9	20.2	1507	1657	1.073	1.084	1617	1796	1.111
6 hog	136.0	171.6	22.6	1206	1522	1.229	1.248	1482	1899	1.281

On the other hand, loss statistics for the world fleet show an annual loss ratio of hull girder collapse due to overloading of less than 0.1% both on tonnage and number of ships [11].

Three major factors can be listed in an attempt to explain this good service experience:

a. Variability in still water loads
b. Overconservative combination of dynamic load components and their combination with still water loads
c. The built-in redundancy margins in the hull girder as discussed herein

Still Water Loads

Most ships are in average not operated up to their maximum design still water loads. Full scale measurements based on Load Master data from a large number of vessels presented in ref. [36] strongly support this fact. For large tankers the discrepancy on mean bending moment may be substantial. As an example the still water moments has been modified for vessel no. (5) and is given in % of the nominal design rule values in Table 6.

The values for still water moments represent reasonable characteristic values (mean + 2*standard deviations) for use in design. As seen for the handy-size tanker (5) this has a significant effect on the Design Capacity Margins.

Load Combinations

There is strong evidence that still water loads and wave loads are being combined in a too concervative way leading to an overprediction of the ship design load moment M_D, ref. [34]. Recent reliability studies carried out for conventional tankers [37] and spherical tank LNG carriers [38] give some indications to this effect. However, more systematic studies have to be carried out in order to determine "universal" load combination factors and load safety factors for use in design codes.

Further, direct calculations of wave loads based on actual still water loads may produce results different from standard rule values. Table 3 and 7 gives an example for vessel (3) for which direct load calculations were carried out. The combined load level was 7.6% less than the rule load combination for a 20 year return period. The maximum wave bending moment was used as a basis with the associated (same time instant values) shear forces, accelerations and pressures. In this case the Design Capacity Margin (RS) increased from 1.25 to 1.35.

Hull Girder Redundancy Margins

From the results it appears that the double bottom vessels, no. (4) and (6), as well as the crude oil carrier (3) have quite good strength margins. The two tankers (2) and (5) have been more optimized to comply with minimum rule

TABLE 6 Effect on Strength Margins from Variability in Still-Water Loads

Loading Condition	$M_{SWD}/M_{SW(nom)}$ [%]	Handy-Size Tanker(5) revised M_U/M_D (RS)	(m)
part-load (hog):	33.0 + 2*21.0 = 75	1.159	1.486
full-load (sag):	-26.3 - 2*21.4 = 69.1	1.126	1.336
all cond. (hog):	-12.4 - 2*21.3 = 55	1.282	1.535

TABLE 7 Design Strength Margins and Current Rule Practise

Vessel no.	Ship Rule design Moment	Panel Moment Capacity margins M_{CR}/M_D		Ultimate Moment Capacity Margins M_U/M_D	
	M_D [MNm]	Ship Rules	MOU CN30.1	(RS)	(m)
2 hog	13142	1.037	0.958	1.064	1.364
3 sag	9610 (8880)1	1.118	1.029	1.248(1.351)	1.508
4 sag	3131	0.986	1.020	1.164	1.416
5 sag	5418	1.008	0.905	1.030	1.223
5 hog	5530	1.050	0.779	1.000	1.282
6 sag	1229	1.151	1.226	1.316	1.461
6 hog	1170	1.132	1.031	1.267	1.623

1 Values in parantheses are based on direct load calculations

strength criteria. Hence, their strength margins are smaller, but still nominally adequate.

For the hogging case of the smaller tanker (5) the ship rule stiffener strength overpredicts the real capacity of the vessel with about 5%, Table 4. This is due to the fact that the ship rule buckling strength procedure does not include the effect of lateral pressure on the buckling strength, whereas this effect has been included in the numerical strength formulation in FENCOL.

Design Code Development

In present ship rules most of the safety factors seem to have been put on the load side. This means that the strength check is perfomed at an artificially high stress level. Extensive full scale measurements from the mid 70'ies tend to support this argument [39], Fig. 9.

Present ship rule procedures undoubtedly work for known designs where in-service experience exist and where this information has been used in code updating and code improvements. Small design changes (design evolutions) have been taken care of by extrapolation from previous experience. However, for unconventional new designs where in-service experience is not available such a procedure may be dangerous and lead to possible overprediction of strengths and safety levels.

The trend in design rule development goes in the direction of reliability based rules formulated within the framework of a partial safety factor (Level I) format. In such a procedure target reliability levels are determined from previous successful designs and damage experience data. In this way rational codes can be developed on a sound theoretical as well as practical basis. When a reasonable target safety level for the new structure has been decided such codes can be extrapolated with more confidence to unconventional (new) designs.

CONCLUSIONS

Ultimate strength predictions are presented in order to provide some quantifications of the overall (longitudinal) ultimate strength margins inherent in conventional ship design practise as laid down in the rules of the major Classification Societies.

Up-to-date and efficient nonlinear Finite Element Methods have been used. The basic numerical formulation and the modelling techniques have been validated at two levels. First, the strength formulation of each basic element, each representing a stiffened plate panel, has been validated with collapse tests on plate strength and with more refined nonlinear analyses. Secondly, the method and modelling has been validated by comparing ULS strength predictions with FENCOL with the test resuls of a large scale box girder and a VLCC that broke her back in harbour due to incorrect unloading operations.

The latter was a very controlled collapse situation well suited for verification of strength predictions. However, depending on the assumptions made on basic strength parameters and modelling uncertainties in the strength formulation a variation from -22% to +15% from the measured collapse moment was experienced. The best estimate was produced by using the mean value strength formulation associated with a nominal yield strength value (2.4% above "test").

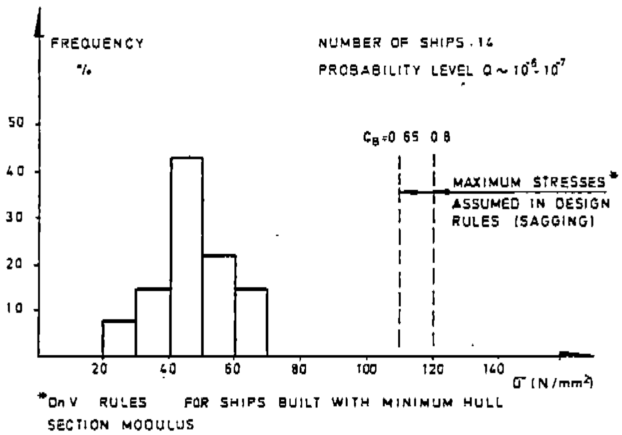


Fig. 9 Measured Wave Bending Stresses in Ship Decks

The spread in hull girder strength margins are quite substantial. This points in the direction of reliability analyses as the only rational approach for structural safety assessment. In this way not only strength margins are considered, but also the uncertainties on the load and load response side.

Some results and procedures w.r.t. hull girder redundancy margins have been given here in a form suitable for reliability analyses.

Results of numerical analyses and mean value strength estimates of 4 additional vessels are presented. The results for single skin vessels indicates that to-days tanker design practise tend to produce small, but still adequate, hull girder rule strength (RS) margins. Mean value stength estimates produce results which are from 11% to 28% higher.

The question whether wave loads are in general overestimated or being combined in a too conservative manner with still water loads is addressed in some detail and indications are given that points in this direction. These questions are currently being addressed in Veritas as well as elsewhere in the world within the framework of reliability analyses.

The double bottom (double skin) vessels (4)(6) as well as the medium sized crude oil carrier (3) have quite satisfactory strength margins. The all hatch cargo vessel (6) has the higest strength margins of the considered vessels. This is partly due to the fact that its midship section scantlings were determined more from local strength criteria than from the hull girder section modulus requirement.

Cleverly used, the minimum rule requirements will in general result in weight minimum designs. With minimum weight designs and the use of HT steel plate thicknesses are in general smaller. This reduces relative corrosion margins leaving less strength reserves.

The use of HT steel leads to higher stress levels which again creates more fatigue problems and a need for better design of detailed areas in order to reduce stress concentrations. Thinner plates yields a more flexible ship in which coatings are more likely to scale off accelerating the corrosion in these areas.

As a consequence more attention have to be paid to structural maintenance and condition monitoring in order to preserve the necessary strength and safety of the ships with increasing ship age.

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DISCUSSION

E. Nikolaidis

I have a comment on the importance of the uncertainties in strength. I remember a paper by Pittaluga named "*Reliability Based Ship Design in the 90's; Realistic Scenario or a Dream?*" In this paper Pittaluga emphasized the importance of knowing uncertainties and limit states and quantifying those uncertainties in reliability analysis and design. This comment supports the work presented in your paper.