GUIDANCE NOTES ON

WHIPPING ASSESSMENT FOR CONTAINER CARRIERS

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Foreword

The main purpose of these Guidance Notes is to supplement the Rules and Guides that ABS has issued for the Classification for container carriers. ABS *Rules for Building and Classing Steel Vessels (Steel Vessel Rules,* or the *Rules)* and the ABS Guide for *Application of Higher-Strength Hull Structural Thick Steel Plates in Container Carriers* require the evaluation of whipping effect on hull structures. These Guidance Notes address how to carry out such evaluations.

These Guidance Notes provide detailed procedures for the assessment of whipping loads and subsequent structure strength for container carriers. The technical background is based on the direct analysis of slamming load and structure dynamic response. These Guidance Notes are applicable to Container Carriers which have hull forms that can be susceptible to whipping effects.

The effective date of this Guide is the first day of the month of publication.

Users are advised to check periodically on the ABS website www.eagle.org to verify that the version of these Guidance Notes is the most current. Comments or suggestions can be sent electronically to *rsd@eagle.org*.

GUIDANCE NOTES ON

WHIPPING ASSESSMENT FOR CONTAINER CARRIERS

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SECTION 1 Introduction

1 General

The design and construction of the hull, superstructure and deckhouses of container carriers are to be based on the applicable requirements of the ABS Rules and Guides. As a supplement to the ABS Rules and Guides, these Guidance Notes provide detailed procedures for whipping assessment for container carriers. The procedure is easy to use and can be utilized to make quick estimates of the whipping effect on hull girder bending moment and on fatigue load. It could be utilized during the conceptual design phase and to perform a sensitivity study of its variation with main dimensions and operational profiles. The technical background is based on the direct analysis of slamming load and structure dynamic response. The procedure has been calibrated using a number of existing designs.

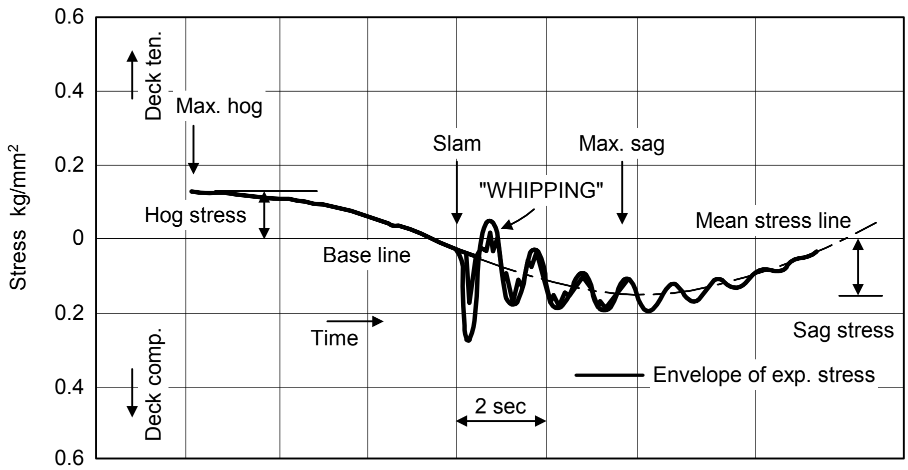
2 Whipping Phenomenon

In rough seas, the ship's bow and stern may occasionally emerge from a wave and re-enter the wave with a heavy impact or slam as the hull structure comes in contact with the water. A ship with such excessive motions is subject to very rapidly developed hydrodynamic loads. The ship will experience impulse loads with high-pressure peaks during the impact between the ship hull and water. Of interest are the impact loads such as bowflare slamming, bottom slamming, stern slamming, green water, and bow impact loads. These impact loads are of a transient nature and can cause severe structural damages.

Impact loads can cause local structural damage due to high impact pressure. They can also induce hull girder vibration mainly in the fundamental 2-node mode. This hull girder vibration is referred to as "whipping", as shown in Section 1, Figure 1. The vibratory hull girder bending stress, or whipping stress, is of much higher frequency than the wave-induced stress, and is effectively superimposed on it. The period of the fundamental mode of whipping is usually in the range from 0.5 to 2 sec.

Whipping has been observed in full scale measurements and can result in an increase of the extreme value of the hull girder bending moment and fatigue damage to structures.

FIGURE 1
Whipping Stress due to Slamming



For vessels possessing significant bowflare or operating at shallow draft, the impact load and the structure response to the impact load need to be evaluated accordingly. The ABS *Guide for Slamming Loads and Strength Assessment for Vessels* provides procedures for the assessment of the impact load and the structure strength due to impact pressures. These Guidance Notes focus on the hull girder response to the impact load, namely whipping effects.

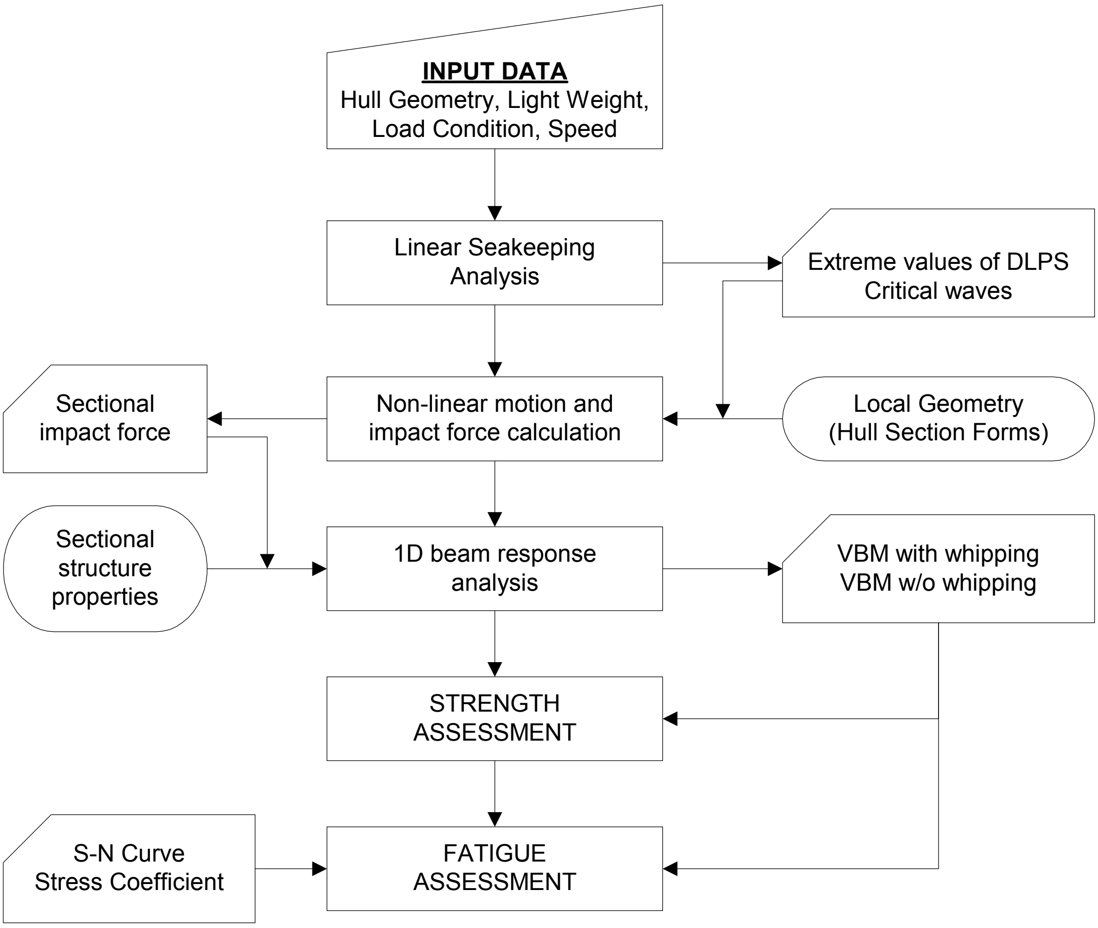
3 Whipping Assessment Procedure

The recommended whipping assessment procedure includes the following:

- Determine the critical loading conditions, forward speed, and operational headings for whipping assessment.
- Select wave environmental data, such as wave scatter diagram and wave spectrum.
- Perform vessel motion analysis.
- Calculate impact load.
- Calculate impact load induced hull girder bending moment.
- Calculate wave load induced hull girder bending moment.
- Determine total hull girder bending moment.
- Perform ultimate hull girder strength assessment.
- Determine whipping induced fatigue damage.
- Determine total fatigue damage.
- Calculate fatigue damage contribution from whipping.

The analysis flowchart is given in Section 1, Figure 2. Detailed descriptions for the analysis procedures are given in Section 2 though Section 8.

FIGURE 2
Whipping Strength Assessment Procedure



SECTION 2 Loading Conditions, Speeds, and Headings

1 General

Impact loads are closely related to the relative motions between the vessel and the water surface. Loading condition, vessel's transit speed, and wave direction affect motions of the vessel and should be selected to cover the critical conditions for impact load and whipping response.

2 Critical Loading Conditions

In general, the critical loading conditions are to be identified based on the susceptibility of the hull structure to impact loads, giving consideration to the fore and aft hull forms, as well as the local drafts relative to the hull structure. Container carriers are, in general, susceptible to bowflare slamming due to large flare angles.

For the strength and fatigue assessment against whipping, two loading conditions are to be considered. One is the seagoing loading condition corresponding to the scantling draft, and the other is the seagoing condition with minimum draft.

3 Standard Speed Profile

In high seas, the ship speed may be reduced voluntarily or involuntarily. If a specific operation profile for the vessel is not available, a standard speed profile is to be applied based on the significant wave height as shown in Section 2, Table 1, where V_d is the design speed.

4 Wave Heading

It is assumed that whipping mainly occurs in bow sea conditions. It is recommended that wave headings of head sea (180-degree), 165-degree and 150-degree bow seas are to be included in the whipping analysis.

TABLE 1
Standard Speed Profile for Slamming Load Prediction

Significant Wave Height, H_s	*Speed*
$0 < H_s \leq 6.0$ m (19.7 ft)	100% V_d
6.0 m (19.7 ft) $< H_s \leq 9.0$ m (29.5 ft)	75% V_d
9.0 m (29.5 ft) $< H_s \leq 12.0$ m (39.4 ft)	50% V_d
12.0 m (39.4 ft) $< H_s$	25% V_d

SECTION 3 Wave Environments

1 Wave Scatter Diagram

As seagoing vessels are typically designed for unrestricted service, the wave scatter diagram from IACS Recommendation 34 is to be employed. Section 3, Table 1 shows the wave scatter diagram, where T_z is the average zero up-crossing wave period and H_s is the significant wave height. The numbers in the diagram represent the probability of sea states described as occurrences per 100,000 observations.

TABLE 1
IACS Recommendation 34 Wave Scatter Diagram for North Atlantic

	T_z (sec)																
H_s (m)	3.5	4.5	5.5	6.5	7.5	8.5	9.5	10.5	11.5	12.5	13.5	14.5	15.5	16.5	17.5	18.5	Sum
16.5	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.1	0.2	0.2	0.2	0.1	0.1	0.0	0.0	0.0	1
15.5	0.0	0.0	0.0	0.0	0.0	0.0	0.1	0.4	0.6	0.7	0.5	0.3	0.1	0.1	0.0	0.0	3
14.5	0.0	0.0	0.0	0.0	0.0	0.1	0.4	1.2	1.8	1.8	1.3	0.7	0.3	0.1	0.0	0.0	8
13.5	0.0	0.0	0.0	0.0	0.0	0.3	1.4	3.5	5.0	4.6	3.1	1.6	0.7	0.2	0.1	0.0	21
12.5	0.0	0.0	0.0	0.0	0.1	1.0	4.4	9.9	12.8	11.0	6.8	3.3	1.3	0.4	0.1	0.0	51
11.5	0.0	0.0	0.0	0.0	0.3	3.3	13.3	26.6	31.4	24.7	14.2	6.4	2.4	0.7	0.2	0.1	124
10.5	0.0	0.0	0.0	0.0	1.2	10.7	37.9	67.5	71.7	51.5	27.3	11.4	4.0	1.2	0.3	0.1	285
9.5	0.0	0.0	0.0	0.2	4.3	33.2	101.9	159.9	152.2	99.2	48.3	18.7	6.1	1.7	0.4	0.1	626
8.5	0.0	0.0	0.0	0.7	15.4	97.9	255.9	350.6	296.9	174.6	77.6	27.7	8.4	2.2	0.5	0.1	1309
7.5	0.0	0.0	0.0	3.0	52.1	270.1	594.4	703.2	524.9	276.7	111.7	36.7	10.2	2.5	0.6	0.1	2586
6.5	0.0	0.0	0.2	12.6	167.0	690.3	1257.9	1268.6	825.9	386.8	140.8	42.2	10.9	2.5	0.5	0.1	4806
5.5	0.0	0.0	1.0	51.0	498.4	1602.9	2372.7	2008.3	1126.0	463.6	150.9	41.0	9.7	2.1	0.4	0.1	8328
4.5	0.0	0.0	6.0	196.1	1354.3	3288.5	3857.5	2685.5	1275.2	455.1	130.9	31.9	6.9	1.3	0.2	0.0	13289
3.5	0.0	0.2	34.9	695.5	3226.5	5675.0	5099.1	2838.0	1114.1	337.7	84.3	18.2	3.5	0.6	0.1	0.0	19128
2.5	0.0	2.2	197.5	2158.8	6230.0	7449.5	4860.4	2066.0	644.5	160.2	33.7	6.3	1.1	0.2	0.0	0.0	23810
1.5	0.0	29.3	986.0	4976.0	7738.0	5569.7	2375.7	703.5	160.7	30.5	5.1	0.8	0.1	0.0	0.0	0.0	22575
0.5	1.3	133.7	865.6	1186.0	634.2	186.3	36.9	5.6	0.7	0.1	0.0	0.0	0.0	0.0	0.0	0.0	3050
Sum	1	165	2091	9280	19922	24879	20870	12898	6245	2479	837	247	66	16	3	1	100000

2 Wave Spectrum

Sea wave conditions are to be modeled by the two-parameter Bretschneider spectrum, which is determined by the significant wave height and the zero-crossing wave period of a sea state. The wave spectrum is given by:

$$S_\zeta(\omega) = \frac{5\omega_p^4 H_s^2}{16\omega^5} \exp\left[-1.25(\omega_p / \omega)^4\right]$$

where

S_ζ = wave energy density, m^2-sec (ft^2-sec)

H_s = significant wave height, m (ft)

ω = angular frequency of wave component, rad/sec

ω_p = peak frequency, rad/sec

= $2\pi/T_p$

T_p = peak period, sec

= $1.408\ T_z$

To consider short-crested waves, "cosine squared" spreading is to be utilized, which is defined as:

$f(\beta) = k \cos^2(\beta - \beta_0)$

where

β = wave heading, following sea is 0 degrees, and head sea is 180 degrees, in the range of $\beta_0 - \dfrac{\pi}{2} \le \beta \le \beta_0 + \dfrac{\pi}{2}$

β_0 = main wave heading of a short-crested wave

k = factor determined such that the summation of $f(\beta)$ is equal to 1.0, i.e.:

= $\displaystyle\sum_{\beta_0-\pi/2}^{\beta_0+\pi/2} f(\beta) = 1$

SECTION 4 Vessel Motions

1 General

This Section describes prediction of vessel motions. The motions of interest for slamming load calculations are relative velocity and relative motion in the vertical direction between waves and the part of the vessel subject to wave impact.

Vessel motions may be obtained either by model testing or through seakeeping analysis. A three-dimensional seakeeping analysis code is to be used for the motion and whipping analysis for final strength assessment. A simplified motion calculation approach can also be utilized in the whipping assessment at an early design stage. The simplified motion calculation will combine with the simplified impact load and whipping analysis approach. The simplified analysis approach is easy to use and requires only main particulars of the design. It could be used during conceptual design for sensitive analysis and be used to narrow down critical cases for further study.

2 Simplified Motion Calculation Approach

The closed-form expressions for the frequency response function for wave induced motions for monohull ships have been derived by Jensen and Mansour (2004). The closed-form expressions can be used as a simplified approach to predict motions and relative motions at an early design stage.

2.1 Motions

The frequency response functions for heave and pitch for the vertical wave-induced motions of a homogeneously loaded box-shaped vessel can be derived analytically by the linear strip theory (Gerritsma and Beukelman, 1964). The equations of motion in regular waves with amplitude, a, can be written as (Jensen, 2001):

$$2\frac{kd}{\omega^2}\ddot{w}+\frac{A^2}{kB\alpha^3\omega}\dot{w}+w=aF\cos(\omega_e t)$$

$$2\frac{kd}{\omega^2}\ddot{\theta}+\frac{A^2}{kB\alpha^3\omega}\dot{\theta}+\theta=aG\sin(\omega_e t)$$

where

k	=	wave number, m^{-1} (ft^{-1})
ω	=	wave frequency, rad/sec
B, d	=	breadth and draft of the box, m (ft)

Differentiation with respect to time, t, is denoted by a dot. The frequency of encounter, ω_e, is given by:

$$\omega_e = \omega - kV\cos\beta = \alpha\omega$$

where

V	=	forward speed, m/sec (ft/sec)
β	=	heading angle (180° corresponding to head sea)
α	=	$1-(kV/\omega)\cos\beta$

The sectional hydrodynamic damping is modeled by the dimensionless ratio A between the incoming and diffracted wave amplitude through the approximation:

$$A = 2\ \sin(0.5kB\alpha^2)\exp(-kd\alpha^2)$$

The forcing functions F and G are given as:

$$F = Cf\,\frac{2}{k_e L}\sin\left(\frac{k_e L}{2}\right)$$

$$G = Cf\,\frac{24}{(k_e L)^2 L}\left[\sin\left(\frac{k_e L}{2}\right) - \frac{k_e L}{2}\cos\left(\frac{k_e L}{2}\right)\right]$$

where

f = $\sqrt{(1-kd)^2 + \left(\frac{A^2}{kB\alpha^3}\right)^2}$

k_e = effective wave number

= $|k \cos \beta|$

C = smith correction factor, approximated by:

= $\exp(-k_e d)$

Solution of motion equations yields the frequency response functions of heave and pitch as:

$$FRF_w = \eta\ |F| \qquad\qquad FRF_\theta = \eta\ |G|$$

where

$$\eta = \left(\sqrt{(1-2kd\alpha^2)^2 + \left(\frac{A^2}{kB\alpha^2}\right)^2}\right)^{-1}$$

For the block coefficient, C_B, less than one, the breadth, B, is replaced by BC_B.

The frequency response function for the vertical motion and velocity in a longitudinal position x from the centre of gravity can be obtained as:

$$FRF_w(x) = \sqrt{FRF_w^2 + x^2 RFR_\theta^2} \qquad\qquad FRF_{vel} = \omega_e FRF_w(x)$$

2.2 Relative Motions

Relative vertical motion $r(x, t)$ with respect to the wave elevation $h(x, t)$ can be expressed as:

$$r(x, t) = w(t) - x\theta(t) - h(x, t)$$

The relative motion is at a position x measured positively from amidships, and its frequency response function FRF_{Rmot} can be derived as:

$$FRF_r = \sqrt{\left(FRF_w - \cos\xi(x)\right)^2 + \left(xFRF_\theta + \sin\xi(x)\right)^2}$$

where

$\xi(x)$ = $\varepsilon_e + \varepsilon_r + k_e x$

ε_e and ε_r are defined by:

$$\cos \varepsilon_e = \frac{1 - kd}{f} \qquad \sin \varepsilon_e = \frac{A^2}{kB\alpha^3 f}$$

$$\cos \varepsilon_r = (1 - 2kd\alpha^2)\eta \qquad \sin \varepsilon_r = -\frac{A^2}{kB\alpha^2}\eta$$

where the symbols have the same definition as in Subsections 4/2.1. The frequency response function for the relative velocity is obtained by multiplication with ω_e to the relative motion.

2.3 Short-term Statistics

The standard deviation, S_R, of a linear wave-induced response, R (Motions, relative motions, etc.), is given as:

$$S_R^2 = \int_0^\infty FRF_R^2(\omega)S(\omega)d\omega$$

where $S(\omega)$ is wave spectra.

3 Three-dimensional Seakeeping Analysis Method

For the final strength assessment of whipping effects, three-dimensional seakeeping analysis is to be applied to determine vessel motions. The seakeeping analysis can be carried out using the frequency domain or time domain approach. In the frequency domain analysis approach, the equations of motion are linearized by assuming the motions to be small and time harmonic. The boundary-value problem is solved using a singularity distribution on the mean body boundary.

An alternative to the frequency domain seakeeping analysis approach is to formulate the time domain initial-value problem. Non-linear effects can be included in time domain analysis approach. It is also convenient using a time domain program to link to the impact load and whipping calculation since they are calculated in time domain due to their transient nature and nonlinearity.

In general, the frequency domain analysis approach is less time consuming compared with the time domain analysis approach. A full spectrum analysis for motions and global hull girder response can be carried out using the frequency domain analysis approach for all wave conditions. The time domain approach can be used for the critical wave conditions.

3.1 General Modeling Considerations

In seakeeping analysis, the wetted body surface is to be partitioned into discrete panels to represent a smoothed body surface. In general, the panel mesh should be fine enough to resolve radiation and diffraction waves with reasonable accuracy.

When generating the panels, care should be taken on the smooth transition of the geometry and the size of the panels. The seakeeping analysis model includes the panel model and the characteristics of weight and waves. For a time domain analysis, free surface is also described using a number of panels.

For each loading condition, the draft at the F.P. and A.P., the location of center of gravity, radii of gyration, and sectional mass distribution along the vessel's length are to be prepared from the Trim and Stability booklet. The free surface GM correction is to be considered for partially filled tanks. For a tank with filling levels above 98% or below 2% of tank height, the free surface GM correction may be ignored.

The evaluation of the seakeeping analysis model should include the following:

3.1.1 Hydrostatic Balance

For each cargo loading condition, the hydrostatics of the vessel calculated based on the panel model are to be verified. At a statically balanced loading condition, the displacement, trim and draft, Longitudinal Center of Buoyancy (LCB), transverse metacentric height (GMT), and longitudinal metacentric height (GML) should be checked against the values provided in trim and stability booklet. The differences should be within the following tolerances:

Displacement:		±1%
Trim:		±0.1 degrees
Draft:		
	Forward	±1 cm (0.4 in.)
	Aft	±1 cm (0.4 in.)
LCB:		±0.1% of length
GMT:		±2%
GML:		±2%
SWBM:		±5%

3.1.2 Hydrodynamic Performance

Each software has its own requirements in term of the panel modeling size relative to wave length, smooth transition from the large panels to smaller panels, panel size near free surface, etc., that are to be adhered to. In general, there should be at least seven panel elements within the wave length. For very short waves, the panel model may not be able to meet that requirement but this does not, in general, affect the results significantly.

3.1.3 Roll Damping

The roll motion of a vessel in beam or oblique seas is greatly affected by viscous roll damping, especially with wave frequencies near the roll resonance. For seakeeping analysis based on potential flow theory, a proper viscous roll damping model is required. Experimental data or empirical methods can be used for the determination of the viscous roll damping. In addition to the hull viscous damping, the roll damping due to rudders and bilge keels is to be considered. If this information is not available, 10% of critical damping may be used for overall viscous roll damping.

3.2 Dominant Load Parameters

Frequency domain analysis is to be carried out for all wave conditions to determine the critical wave conditions for further time domain analysis. The critical wave conditions are determined based on the response of dominant load parameters. For the whipping assessment, the dominant load parameters are:

- Relative velocity at FP
- Relative velocity at AP
- Relative motion at FP
- Relative motion at AP
- Vertical bending moment at midship

In frequency domain analysis, the Response Amplitude Operators are first to be calculated for the dominant load parameters for loading conditions specified in Section 2/2 and all wave conditions. These dominant load parameters will be considered for the calculation of short-term and long-term analysis to determine the critical wave conditions.

A sufficient range of wave headings and frequencies should be considered for the calculation of the short-term extreme value of each relative velocity. The Response Amplitude Operators are to be calculated for wave headings from head seas (180 degrees) to following seas (0 degrees) in increments of 15 degrees. The range of wave frequencies is to include at least from 0.2 rad/s to 1.20 rad/s in increments of 0.05 rad/s.

3.3 Critical Wave Conditions

The critical wave conditions are determined based on the extreme values of the dominant load parameters and the response amplitude operator of the dominant load parameters.

3.3.1 Extreme Value of Dominant Load Parameter

The extreme values of dominant load parameters can be predicted using a long-term analysis approach. The long-term analysis probability of the response exceeding x_0, $\Pr\{x_0\}$ may be presented by the following equation, expressed as a summation of joint probability over the short-term sea states:

$$\Pr\{x_0\} = \sum_i \sum_j p_i p_j \Pr_j\{x_0\}$$

where

p_i	=	probability of the *i*-th main wave heading angle
p_j	=	probability of occurrence of the *j*-th sea state defined in wave scatter diagram
$\Pr_j\{x_0\}$	=	probability of the short-term response exceeding x_0 for the *j*-th sea state

For the calculation of long-term response of a vessel in unrestricted service, equal probability of main wave headings may be assumed for p_i. The long-term probability $\Pr\{x_0\}$ is related to the total number of response cycles in which the relative velocity is expected to exceed the value x_0. Denoted by N, total number of cycles, the relationship between the long-term probability $\Pr\{x_0\}$ and N can be expressed by the following equation:

$$\Pr\{x_0\} = \frac{1}{N}$$

The term $1/N$ is often referred to as the exceedance probability level. Using the relationship given by the last equation, the response exceeding the value x_0 can be obtained at a specific probability level. The relevant value to be obtained from the long-term spectral analysis is the extreme value at the exceedance probability level of 10^{-8}. This probability level ordinarily corresponds to the long-term response of 20 ~ 25 years.

3.3.2 Critical Wave Conditions

The critical wave conditions are defined here as regular waves that simulate the long-term extreme values of the Dominant Load Parameters. The critical waves (or the equivalent design waves) can be characterized by wave amplitude, wave length, wave heading, and wave crest position referenced to amidships. For each of the Dominant Load Parameters, an equivalent design wave is to be determined.

3.3.2(a) Equivalent Wave Amplitude. The wave amplitude of the equivalent design wave is to be determined from the long-term extreme value of a Dominant Load Parameter under consideration divided by the maximum RAO amplitude of that Dominant Load Parameter. The maximum RAO occurs at a specific wave frequency and wave heading where the RAO has its maximum value. Equivalent wave amplitude (EWA) for the *j*-th Dominant Load Parameter may be expressed by the following equation:

$$a_w = \frac{LTR_j}{RAO_j^{\max}}$$

where

a_w	=	equivalent wave amplitude of the *j*-th Dominant Load Parameter, m (ft)
LTR_j	=	long-term response of the *j*-th Dominant Load Parameter
$RAO_j^{\max}$	=	maximum RAO amplitude of the *j*-th Dominant Load Parameter

3.3.2(b) Wave Frequency and Heading. The wave frequency and heading of the equivalent design wave, denoted by (ω, β), are to be determined from the maximum RAO of each Dominant Load Parameter. The wave length of the equivalent design wave can be calculated by the following equation:

$$\lambda = (2\pi g)/\omega^2$$

where

λ	=	wave length, m, (ft)
g	=	gravitational acceleration, m/sec^2 (ft/sec^2)
ω	=	wave frequency, rad/sec

3.4 Time Domain Analysis

For the selected critical waves, the ship motion analysis is to be performed in the time domain. The ship motions are to include all six degrees of freedom rigid-body motions. The hull may be assumed as a rigid body and the global rigid-body motion may not be altered by local slamming load. The time simulation is to be performed until the response reaches its steady state.

In the time domain analysis approach, the free surface is to be described by a number of panel elements covering a large area of the surface near the floating vessel. In general, there are two numerical methods for time domain analysis: mixed-source formulation and Rankine source formulation. Each approach has its own requirements on the area of the free surface that should be modeled in the analysis. Engineers should follow the recommendations from the user manual of the selected programs. In general, the Rankine source formulation approach may require a larger free surface domain than that of the mixed-source formulation approach.

From the time domain analysis, the six degrees of motions and the relative motions of the vessel to the waves are calculated. The time history of those motions will be utilized in the further impact load and whipping response predictions.

SECTION 5 Whipping Response – A Simplified Method

1 General

Whipping is defined as a hull girder vibration induced by impact load. Whipping response includes the hull girder bending moments, hull girder shear force, hull girder deflection, hull girder acceleration, etc. These Guidance Notes focus on the response of vertical bending moment of container carriers.

This Section provides a simplified method for the prediction of whipping response. The method can provide a rational prediction of the hull girder bending moments with sufficient engineering accuracy. At the conceptual design phase, the procedure can be used to make quick estimates and to perform sensitivity analyses by varying main dimensions and operation profiles.

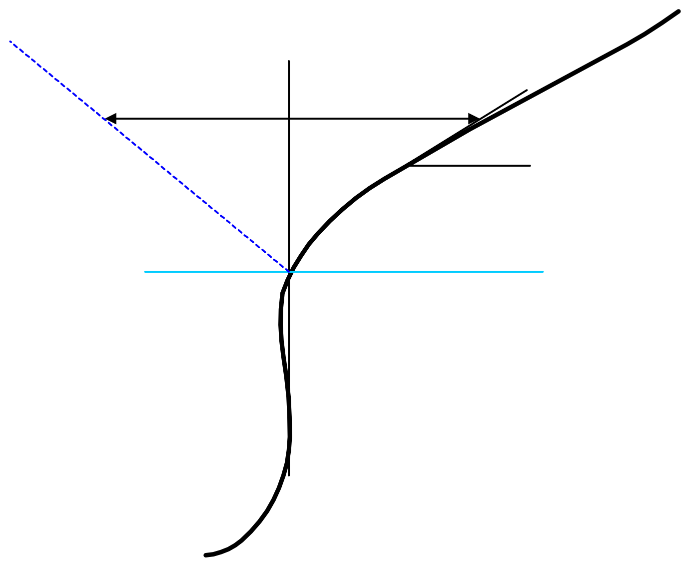
The required input information for the simplified whipping assessment method is restricted to main particulars as follows:

- Length of the vessel
- Molded breadth
- Draft for a selected loading condition
- Block coefficient
- Depth
- Bow flare coefficient
- Local dead rise angle, breadth, and draft at location of impact
- 2-node modal natural period and relative damping
- Operational profile in terms of speed, heading and sea state
- Data for SN curve for fatigue damage estimation

2 Bowflare Impact Load

The impact load is closely related to the hull geometry and the relative velocity between the hull and water. The bowflare geometry can be approximately treated as a wedge (see Section 5, Figure 1).

FIGURE 1
Bowflare Geometry Model



The instantaneous impact force per unit length on a wedge can be predicted by a simplified formula as:

$$q(t) = 3C_p \rho g \dot{z}^3 t \quad \text{kN (tf, Ltf)}$$

where

C_p = $\dfrac{\pi^2}{4\tan^2\alpha}$

ρ = density of sea water, t/m^3 (Lt/m^3)

g = acceleration of gravity, m/s^2 (ft/s^2)

α = deadrise angle of wedge section, or flare angle, rad

$\dot{z}$ = vertical relative velocity at the slamming location, m/sec (ft/sec)

t = time measured from the wedge apex hitting the water, sec

For slamming, the time at maximum immersion including accounting for the water rise-up on a section with deadrise angle, α, and local breadth, B_l, can be approximated by:

$$t_s = \frac{2}{3}\frac{B_\ell \tan\alpha}{2\dot{z}}, \text{ sec}$$

taking into account that the water rise-up is about half the immersion of the section according to the results shown by Zhao and Faltinsen (1993).

In this simple calculation formula, the important input parameter is the vertical position of the section which generates the bow flare slamming impact, or local breadth, B_l, (see Section 5, Figure 2). This position or local breadth will depend on impact velocities, the flare angle and others. Numerical calibration may be needed to determine appropriate values.

To decide the local breadth, B_l, the ship body can be assumed as the water entry of a two-dimensional wedge body. Local breadth can be calculated approximately by:

$$B_\ell = \frac{\Delta H}{\tan\alpha} \quad \text{m (ft)}$$

where

ΔH = 0.75(Depth – Draft), m (ft)

The local section at longitudinal location of $x = 0.95L$ can be selected to represent the slamming section. The deadrise angle, α, is measured for the wedge section four equal-distance points on the slamming section from the draft to 0.75 of the distance between the depth and draft (see Section 5, Figure 3). The average of these angles is the deadrise angle that will be used to calculate the local breadth.

FIGURE 2
Deadrise Angle and Local Breadth of Wedge Section

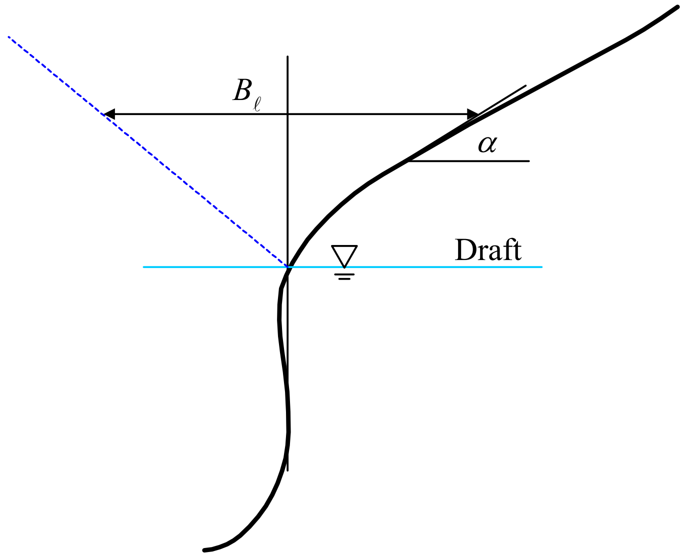
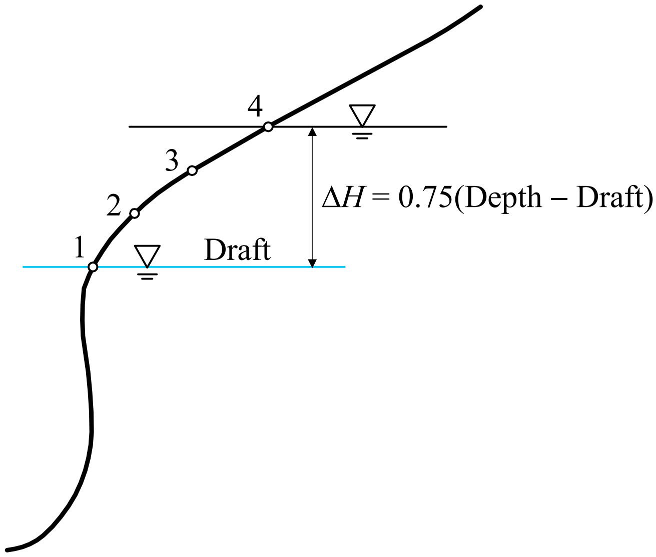


FIGURE 3
Calculation of Deadrise Angle and Local Breadth



3 Vertical Bending Moment due to Whipping

Full-scale measurements have shown that the whipping deflection is primarily in the form of the lower hull girder vibration mode, 2-node vertical vibration mode, in addition to heave and pitch. By using the 2-node mode in the calculation of the whipping bending moments along the length of the vessel, an approximate solution for the whipping bending moment $M(x)$ at $X = x$ following a slam at $X = x_0$ can be derived by the modal superposition method.

3.1 Response of 2-Node Hull Girder Mode under Impact Loads

It is assumed that the duration of the impact is so short that it is not in resonance with heave and pitch modes. Using only the 2-node mode, the vertical whipping deflection of the beam can then be written:

$$u(x, t) = Cw(x)y(t)$$

where $w(x)$ is 2-node mode shape. For a beam with tapered mass and stiffness properties towards the beam ends, the 2-node mode shape can be approximately written as:

$$w(x) = 7.95\left(\frac{x}{L}\right)^3 - 1.23\left(\frac{x}{L}\right)^2 - 4.58\left(\frac{x}{L}\right) + 1 \qquad 0 \le \frac{x}{L} \le 0.5$$

$$w\left(x + \frac{L}{2}\right) = w\left(\frac{L}{2} - x\right)$$

The mode shape is normalized such that $w(0) = 1$. Furthermore, it is assumed that the total inertia force and moment on the vessel are zero, i.e.:

$$\int_0^L m(x)w(x)dx = 0 \qquad \int_0^L m(x)w(x)xdx = 0$$

where $m(x)$ is the mass per unit length. With the impact load, P, occurring at $x = x_0$ and with a longitudinal extend of βL:

$$C = \frac{1}{\Omega^2 M_0}\int_0^L P(x)w(x)dx \cong P\beta L\frac{w(x_0)}{\Omega^2 M_0}$$

$$M_0 = \int_0^L m(x)w^2(x)dx$$

The vertical bending moment after the impact load can be written as:

$$M_{whipping}(x, t) = P\beta L\frac{S(x)}{M_0}w(x_0)y(t)$$

$$S(x) = \int_x^L m(u)w(u)(u - x)du$$

For a non-uniform beam, $S(x)$ can be described as:

$$\frac{S(x)}{LM_0} = -5.4\left(\frac{x}{L}\right)^4 + 1.55\left(\frac{x}{L}\right)^3 + 1.53\left(\frac{x}{L}\right)^2 - 0.09\left(\frac{x}{L}\right) \qquad 0 \le \frac{x}{L} \le 0.5$$

$$S\left(x + \frac{L}{2}\right) = S\left(\frac{L}{2} - x\right)$$

Assuming the duration of the impact is very short, the maximum value in time of the response (dynamic amplification factor) can be approximated as:

$$y_{\max}(t_T) \approx \frac{1}{2}\Omega T = DAF$$

3.2 Whipping Bending Moment

Utilizing the impact load formula from Subsection 5/2, the whipping bending moment can be calculated by:

$$M_{whipping}(x, x_0) = \frac{\pi^2}{24}\rho g B_\ell^2 \Omega \dot{z} \beta L \frac{S(x)}{M_0} w(x_0) \quad \text{kN-m (tf-m, Ltf-ft)}$$

Considering the phase difference between the whipping bending moment and the bending moment by the normal wave load, the whipping bending moment is:

$$M_{whipping}(x, x_0) = \frac{\pi^2}{24}\rho g B_\ell^2 \Omega \dot{z} \beta L \frac{S(x)}{M_0} w(x_0)\exp(-\xi\Lambda\varphi) \quad \text{kN-m (tf-m, Ltf-ft)}$$

where

ξ = relative damping for 2-node vibration mode. ξ can be approximated as 1.5% if no measurement value is available

φ = phase lag between whipping bending moment and wave bending moment. φ can be taken as 30-degree; the extent of impact, rad

βL can be taken as $0.04L$

Ω = natural frequency of 2-node vibration mode of the hull girder, rad/s

L = vessel length, m (ft)

Λ = T_z/T_2

T_z = zero up-crossing wave period of the stationary sea state, sec

T_2 = natural period of 2-node vibration mode of the hull girder, sec

Other parameters are as defined in Subsection 5/2.

The whipping bending moment depends linearly on the relative vertical velocity. The standard deviation of the whipping bending moment can be expressed as the function of the standard deviation of the relative vertical velocity as:

$$S_{m,whipping}(x, x_0) = \frac{\pi^2}{24}\rho g B_\ell^2 \Omega S_{\dot{z}} \beta L \frac{S(x)}{M_0} w(x_0)\exp(-\xi\Lambda\varphi) \quad \text{kN-m (tf-m, Ltf-ft)}$$

where $S_{\dot{z}}$ is the standard deviation of the relative vertical velocity.

The standard deviation of whipping bending moment can be directly added to the standard deviation of wave bending moment since the phase has been considered. A simplified method for the calculation of wave bending moment is given in Appendix 1.

3.2.1 Maximum Bending Moment

The maximum total bending moment is defined as the maximum bending moment that occurs once in 20 years. The probability of the occurrence is about 10^{-8}. A short-term and long-term approach can be used to obtain the maximum bending moment. The short-term approach is presented in this Section.

In the short-term approach, the sea states that occur once in 20 years, or 20-year wave contour, are first selected. Section 5, Figure 4 shows 1-year, 20-year, and 40-year return sea states selected from the wave scatter diagram. Section 5, Table 1 shows the significant wave heights from the sea states with different return periods.

3.2.2 Sagging Bending Moment

Due to non-linearity and impact load, the peaks of sagging bending moment become much bigger than that of hogging bending moment. Therefore, the extreme values of sagging bending moment and hogging bending moment may be calculated separately.

For sagging bending moment, the probability, P, that an individual peak, M, exceeds a given level, m, can be evaluated by:

$$P(M > m) = \exp\left(-\frac{1}{2}u^2(m)\right)$$

where u is given by:

$$u = \frac{-1 + \sqrt{1 + 4\chi(\chi + \overline{m})}}{2\chi}$$

where

χ = $\dfrac{\hat{\kappa}_3}{6}$

$\overline{m}$ = $\dfrac{m}{(S_{m,wave} + S_{m,whipping})r}$

r = $\left(1 + \dfrac{\hat{\kappa}_3^2}{18}\right)^{-\frac{1}{2}}$

$\hat{\kappa}_3$ = $\kappa_3 + \dfrac{6}{\sqrt{2}}\dfrac{S_{m,whipping}}{rS_{m,wave}}$

κ_3 = $0.26 H_s C_f \left(1 - \exp\left(\dfrac{-10F_n}{|\cos\alpha|}\right)\right)\min\left(\dfrac{T_z - 5}{5}, 1\right)$

F_n = $\dfrac{V}{\sqrt{gL}}$

H_s = significant wave height, m (ft)

T_z = zero up-crossing wave period, sec

α = wave heading, rad

V = vessel speed, m/sec (ft/sec)

L = vessel length, m (ft)

C_f = difference between the upper deck area and the water plane area in the forward 20% of the vessel's hull normalized by the freeboard in this area and with the vessel length

$S_{m,wave}$ = standard deviation of bending moment, kN-m (tf-m, Ltf-ft), due to linear normal wave load. A simplified method for the calculation of bending moment due to linear normal wave load is given in Appendix 1.

FIGURE 4
1, 20, 40-Year Return Sea States from IACS Recommendation 34

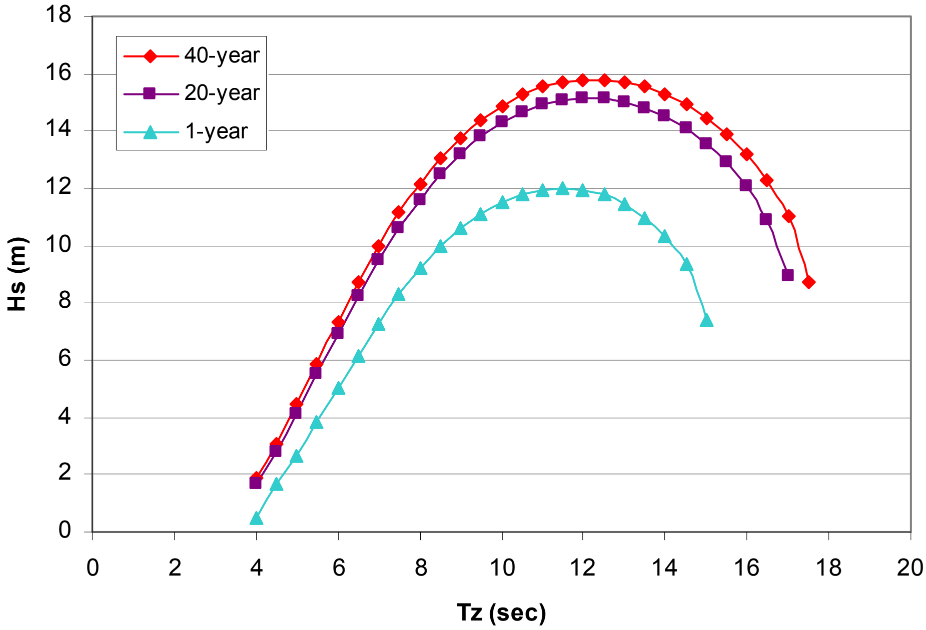


TABLE 1
Sea States from IACS Recommendation 34

T_z(sec)	H_s (m)				
	1-year	20-year	25-year	30-year	40-year
4.0	0.5	1.7	1.7	1.8	1.9
4.5	1.6	2.8	2.9	3.0	3.1
5.0	2.7	4.1	4.2	4.3	4.4
5.5	3.8	5.5	5.6	5.7	5.9
6.0	5.0	6.9	7.0	7.1	7.3
6.5	6.2	8.2	8.4	8.5	8.7
7.0	7.3	9.5	9.6	9.8	10.0
7.5	8.3	10.6	10.8	10.9	11.1
8.0	9.2	11.6	11.8	11.9	12.1
8.5	10.0	12.5	12.6	12.8	13.0
9.0	10.6	13.2	13.4	13.5	13.8
9.5	11.1	13.8	14.0	14.1	14.4
10.0	11.5	14.3	14.5	14.6	14.9
10.5	11.8	14.6	14.8	15.0	15.2
11.0	12.0	14.9	15.1	15.3	15.5
11.5	12.0	15.1	15.3	15.4	15.7
12.0	12.0	15.1	15.4	15.5	15.8
12.5	11.8	15.1	15.3	15.5	15.8
13.0	11.5	15.0	15.2	15.4	15.7
13.5	11.0	14.8	15.0	15.2	15.5
14.0	10.3	14.5	14.7	15.0	15.3
14.5	9.3	14.1	14.4	14.6	14.9
15.0	7.4	13.6	13.9	14.1	14.5
15.5		12.9	13.2	13.5	13.9
16.0		12.0	12.4	12.7	13.2
16.5		10.9	11.4	11.7	12.3
17.0		8.9	9.7	10.3	11.0
17.5					8.7

3.2.3 Hogging Bending Moment

Based on numerical simulation, the initial peak of the whipping induced bending moment starts slightly ahead of the peak of the sagging bending moment. The whipping bending moment oscillates and decays until the next impact occurs. The total maximum hogging bending moment, for a given probability of exceedance, including whipping can be estimated by:

$$M_{hog} = M_{wave} + M_{whipping} \exp(-\xi\omega_n L/2) \quad \text{kN-m (tf-m, Ltf-ft)}$$

where

M_{wave} = maximum bending moment of normal wave component, kN-m (tf-m, Ltf-ft)

ξ = percentage of critical damping

ω_n = 2-node vibration natural frequency, rad

L = vessel length, m (ft)

$M_{whipping}$ = whipping contribution to sagging bending moment, kN-m (tf-m, Ltf-ft), which is the total dynamic sagging bending moment minus the sagging bending moment due to the wave component (see Appendix 1 for wave sagging bending moment).

SECTION 6 Whipping Response – Time Domain Numerical Approach

1 General

In the time domain numerical analysis approach, the impact load can be calculated using a 2-D approach. In this approach, ships are represented by a series of 2-D sections. The motions of each 2-D section (or cut) relative to the free surface are computed from the six degrees of freedom ship motion history and the incident wave definition. Sectional impact forces are then computed for each section in the time domain.

The prediction of whipping, in general, includes three major steps:

- Prediction of ship motions that are assumed to be independent of the impact problem (Subsection 4/3)
- Calculation of a time history of the impact forces (Subsection 6/2)
- Calculation of the main girder response, including loads, to the impact forces using a 1-D finite element beam model (Subsection 6/3)

2 Impact Load

An impact load is generated on the bow flare as it enters an oncoming wave system which produces a hull shudder and subsequent vibratory response.

In a 2-D impact load analysis method, the impact load can be computed by a generalized Wagner solution where the exact body boundary condition is satisfied on the body surface. The pile-up of the surface due to impact is accounted for when calculating the points of intersection between the free surface and the body surface.

In general, the impact forces computation procedure includes the following steps:

- Selection of the 2-D sections that are subject to impact load. In general, the forward part of 0.25*L* from FP and afterward part of 0.15*L* from AP should be considered for the impact load calculation.
- Input of incident wave specification and the computed rigid body ship motions.
- For each selected section at each time step:
  - Compute the incident wave elevation and velocity at the section
  - Compute relative displacement and relative velocity
  - Compute impact load at the input time step
- Output impact load time history for all sections.

3 Whipping Response

Whipping is defined as impact load induced vibration. Since the impact loads are of high intensity and short duration, the response of the ship to the impact load can be decoupled from its response to wave frequency loads. Thus, the slamming response can be modeled as vibration of an elastic beam subject to pure impulse loadings. The calculated sectional forces at a given beam cross section can be superimposed on to the sectional forces due to regular wave frequency loads.

In general, whipping can be calculated using a time integration method or modal superposition method. The computation procedure includes the following steps:

- Input sectional structure properties of the vessel.
- Generate equivalent 1-D finite element beam model.
- Calculate the sectional added masses of the beam model.
- Calculate beam response to the impact load using finite element method.
- Calculate section load (shear force and bending moment).

SECTION 7 Strength Assessment

1 General

The total vertical bending moment including whipping load may be verified for compliance with the hull girder ultimate strength requirements. This Section presents the evaluation procedures for ultimate hull girder strength.

2 Vertical Hull Girder Ultimate Limit State

The vertical hull girder ultimate bending capacity is to satisfy the following limit state equation:

$$\gamma_S M_{sw} + \gamma_W M_w \leq \frac{M_U}{\gamma_R}$$

where

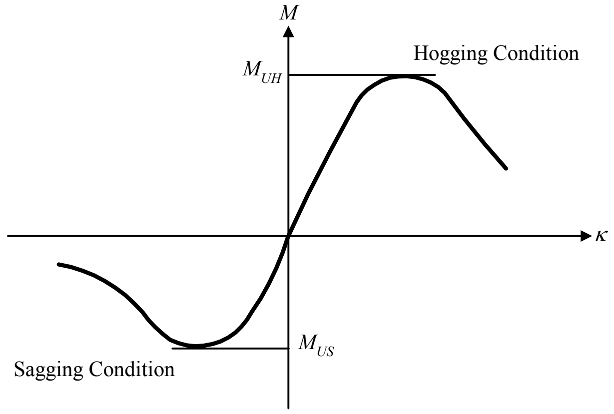
M_{sw}	=	still water bending moment, kN-m (tf-m, Ltf-ft)	
M_w	=	maximum wave-induced bending moment including whipping, kN-m (tf-m, Ltf-ft)	
M_U	=	vertical hull girder ultimate bending capacity, kN-m (tf-m, Ltf-ft)	
γ_S	=	1.0	partial safety factor for the still water bending moment
γ_w	=	1.20	partial safety factor for the vertical wave bending moment
γ_R	=	1.10	partial safety factor for the vertical hull girder bending capacity

3 Hull Girder Ultimate Bending Moment Capacity

3.1 General

The ultimate bending moment capacities of a hull girder section, in hogging and sagging conditions, are defined as the maximum values (positive M_{UH}, negative M_{US}) on the static nonlinear bending moment-curvature relationship M-κ. See Section 7, Figure 1. The curve represents the progressive collapse behavior of the hull girder under vertical bending. Hull girder failure is controlled by buckling, ultimate strength and yielding of longitudinal structural elements.

FIGURE 1
Bending Moment – Curvature Curve M-κ



The curvature of the critical inter-frame section, κ, is defined as:

$$\kappa = \frac{\theta}{\ell} \quad m^{-1}\ (ft^{-1})$$

where:

θ	=	relative angle rotation of the two neighboring cross-sections at transverse frame positions
ℓ	=	transverse frame spacing, m (ft), (i.e., span of longitudinals)

The method for calculating the ultimate hull girder capacity is to identify the critical failure modes of all main longitudinal structural elements.

Longitudinal structural members compressed beyond their buckling limit have reduced load carrying capacity. All relevant failure modes for individual structural elements, such as plate buckling, torsional stiffener buckling, stiffener web buckling, lateral or global stiffener buckling and their interactions, are to be considered in order to identify the weakest inter-frame failure mode.

The effects of shear force, torsional loading, horizontal bending moment and lateral pressure are neglected.

3.2 Physical Parameters

For the purpose of describing the calculation procedure in a concise manner, the physical parameters and units used in the calculation procedure are given below.

3.2.1 Hull Girder Load and Cross Section Properties

M_i	=	hull girder bending moment, kN-m (tf-m, Ltf-ft)
F_i	=	hull girder longitudinal force, kN (tf, Ltf)
I_v	=	hull girder moment of inertia, m^4 (ft^4)
SM	=	hull girder section modulus, m^3 (ft^3)
SM_{dk}	=	elastic hull girder section modulus at deck at side, m^3 (ft^3)
SM_{kl}	=	elastic hull girder section modulus at bottom, m^3 (ft^3)
κ	=	curvature of the ship cross section, m^{-1} (ft^{-1})
z_j	=	distance from baseline, m (ft)

3.2.2 Material Properties

σ_{yd} = specified minimum yield stress of the material, N/cm^2 (kg/cm^2, lbf/in^2)

E = Young's modulus for steel, 2.06×10^7 N/cm^2 (2.1×10^6 kg/cm^2, 30×0^6 psi)

ν = Poisson's ratio, may be taken as 0.3 for steel

Φ = edge function as defined in 7/3.6.2

ε = relative strain defined in 7/3.6.2

3.2.3 Stiffener Sectional Properties

The properties of a longitudinal's cross section are shown in Section 7, Figure 2.

A_s = sectional area of the longitudinal or stiffener, excluding the associated plating, cm^2 (in^2)

b_1 = smaller outstanding dimension of flange with respect to centerline of web, cm (in.)

b_f = total width of the flange/face plate, cm (in.)

d_w = depth of the web, cm (in.)

t_p = net thickness of the plating, cm (in.)

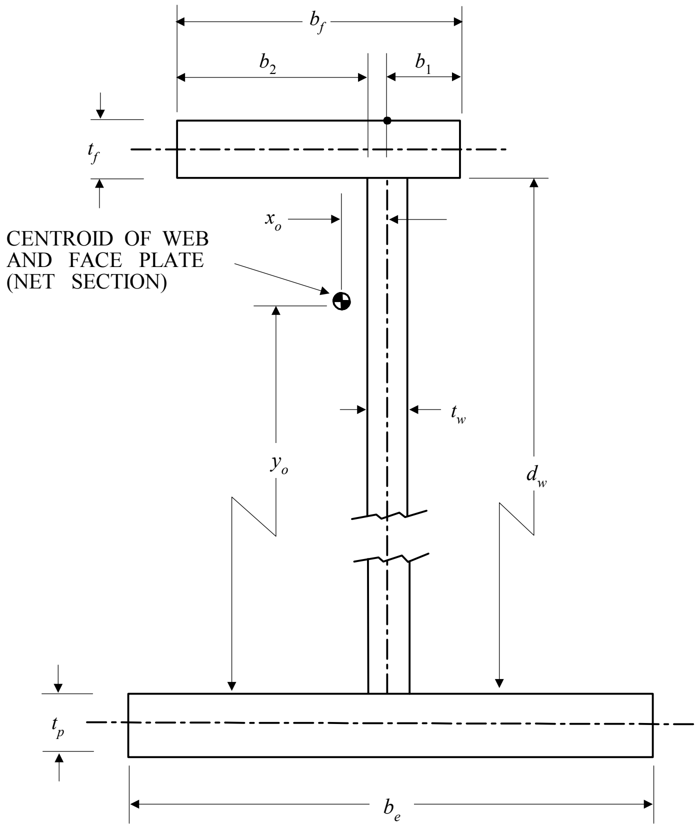
t_f = net thickness of the flange/face plate, cm (in.)

t_w = net thickness of the web, cm (in.)

x_o = distance between centroid of the stiffener and centerline of the web plate, cm (in.)

y_o = distance between the centroid of the stiffener and the attached plate, cm (in.)

FIGURE 2
Dimensions and Properties of Stiffeners



3.3 Calculation Procedure

The ultimate hull girder bending moment capacity M_U is defined as the peak value of the curve with vertical bending moment M versus the curvature κ of the ship cross section as shown in Section 7, Figure 1.

The curve M-κ is obtained by means of an incremental-iterative approach. The steps involved in the procedure are given below.

The bending moment M_i which acts on the hull girder transverse section due to the imposed curvature κ_i is calculated for each step of the incremental procedure. This imposed curvature corresponds to an angle of rotation of the hull girder transverse section about its effective horizontal neutral axis, which induces an axial strain ε in each hull structural element.

The stress σ induced in each structural element by the strain ε is obtained from the stress-strain curve σ-ε of the element, which takes into account the behavior of the structural element in the nonlinear elasto-plastic domain.

The force in each structural element is obtained from its area times the stress and these forces are summed to derive the total axial force on the transverse section. Note the element area is taken as the total net area of the structural element. This total force may not be zero as the effective neutral axis may have moved due to the nonlinear response. Hence, it is necessary to adjust the neutral axis position, recalculate the element strains, forces and total sectional force, and iterate until the total force is zero.

Once the position of the new neutral axis is known, then the correct stress distribution in the structural elements is obtained. The bending moment M_i about the new neutral axis due to the imposed curvature κ_i is then obtained by summing the moment contribution given by the force in each structural element.

The main steps of the incremental-iterative approach are summarized as follows:

Step 1 Divide the hull girder transverse section into structural elements, (i.e., longitudinal stiffened panels (one stiffener per element), hard corners and transversely stiffened panels), see 7/3.4.

Step 2 Derive the stress-strain curves (also known as the load-end shortening curves) for all structural elements, see 7/3.5.

Step 3 Derive the expected maximum required curvature, κ_F. The curvature step size $\Delta\kappa$ is to be taken as $\kappa_F/300$. The curvature for the first step, κ_1 is to be taken as $\Delta\kappa$.

Derive the neutral axis z_{NA-i} for the first incremental step (i = 1) with the value of the elastic hull girder section modulus.

Step 4 For each element (index j), calculate the strain $\varepsilon_{ij} = \kappa_i(z_j - z_{NA-i})$ corresponding to κ_i, the corresponding stress σ_j, and hence the force in the element $\sigma_j A_j$. The stress σ_j corresponding to the element strain ε_{ij} is to be taken as the minimum stress value from all applicable stress-strain curves σ-ε for that element.

Step 5 Determine the new neutral axis position z_{NA-i} by checking the longitudinal force equilibrium over the whole transverse section. Hence, adjust z_{NA-i} until:

$$F_i = 10^{-3}\,\Delta A_j \sigma_j = 0$$

Note σ_j is positive for elements under compression and negative for elements under tension. Repeat from Step 4 until equilibrium is satisfied. Equilibrium is satisfied when the change in neutral axis position is less than 0.0001 m.

Step 6 Calculate the corresponding moment by summing the force contributions of all elements as follows:

$$M_i = 10^{-3} \sum \sigma_j A_j \left|(z_j - z_{NA-i})\right|$$

Step 7 Increase the curvature by $\Delta\kappa$, use the current neutral axis position as the initial value for the next curvature increment and repeat from Step 4 until the maximum required curvature is reached. The ultimate capacity is the peak value M_u from the M-κ curve. If the peak does not occur in the curve, then κ_F is to be increased until the peak is reached.

The expected maximum required curvature κ_F is to be taken as:

$$\kappa_F = 3\,\frac{\max\left(SM_{dk}\sigma_{yd}, SM_{kl}\sigma_{yd}\right)}{EI_v}$$

3.4 Assumptions and Modeling of the Hull Girder Cross-section

In applying the procedure described in this Section, the following assumptions are to be made:

- The ultimate strength is calculated at a hull girder transverse section between two adjacent transverse webs.
- The hull girder transverse section remains plane during each curvature increment.
- The material properties of steel are assumed to be elastic, perfectly plastic.
- The hull girder transverse section can be divided into a set of elements which act independently of each other.

The elements making up the hull girder transverse section are:

- Longitudinal stiffeners with attached plating, with structural behavior given in 7/3.5.2, 7/3.5.3, 7/3.5.4, 7/3.5.5, and 7/3.5.6
- Transversely stiffened plate panels, with structural behavior given in 7/3.5.7
- Hard corners, as defined below, with structural behavior given in 7/3.5.1

The following structural areas are to be defined as hard corners:

- The plating area adjacent to intersecting plates
- The plating area adjacent to knuckles in the plating with an angle greater than 30 degrees.
- Plating comprising rounded gunwales

An illustration of hard corner definition for girders on longitudinal bulkheads is given in Section 7, Figure 3.

The size and modeling of hard corner elements is to be as follows:

- It is to be assumed that the hard corner extends up to $s/2$ from the plate intersection for longitudinally stiffened plate, where s is the stiffener spacing
- It is to be assumed that the hard corner extends up to $20t_{grs}$ from the plate intersection for transversely stiffened plates, where t_{grs} is the gross plate thickness.For transversely stiffened plate, the effective breadth of plate for the load shortening portion of the stress-strain curve is to be taken as the full plate breadth (i.e., to the intersection of other plates – not from the end of the hard corner). The area is to be calculated using the breadth between the intersecting plates.

FIGURE 3
Example of Defining Structural Elements

a) Example showing side shell, inner side and deck

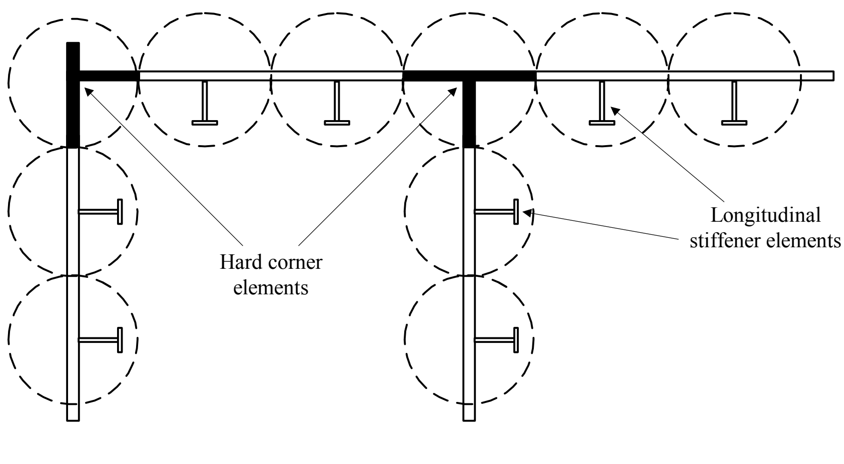
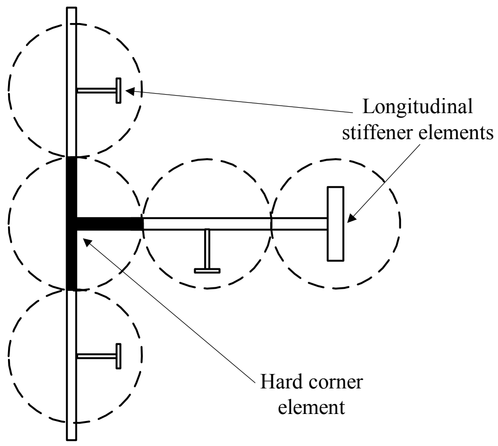


FIGURE 3 (continued)
Example of Defining Structural Elements

b) Example showing girder on longitudinal bulkhead



3.5 Stress-strain Curves σ-ε (or Load-end Shortening Curves)

3.5.1 Hard Corners

Hard corners are sturdier elements which are assumed to buckle and fail in an elastic, perfectly plastic manner. The relevant stress strain curve σ-ε is to be obtained for lengthened and shortened hard corners according to 7/3.5.2.

3.5.2 Elasto-Plastic Failure of Structural Elements

The equation describing the stress-strain curve σ-ε of the elasto-plastic failure of structural elements is to be obtained from the following formula, valid for both positive (compression or shortening) of hard corners and negative (tension or lengthening) strains of all elements (see Section 7, Figure 4):

$$\sigma = \Phi\, \sigma_{yd} \quad \text{N/cm}^2 \text{ (kg/cm}^2\text{, lbf/in}^2\text{)}$$

where

Φ = edge function

= -1 for $\varepsilon < -1$

= ε for $-1 < \varepsilon < 1$

= 1 for $\varepsilon > 1$

ε = relative strain

= $\dfrac{\varepsilon_E}{\varepsilon_{yd}}$

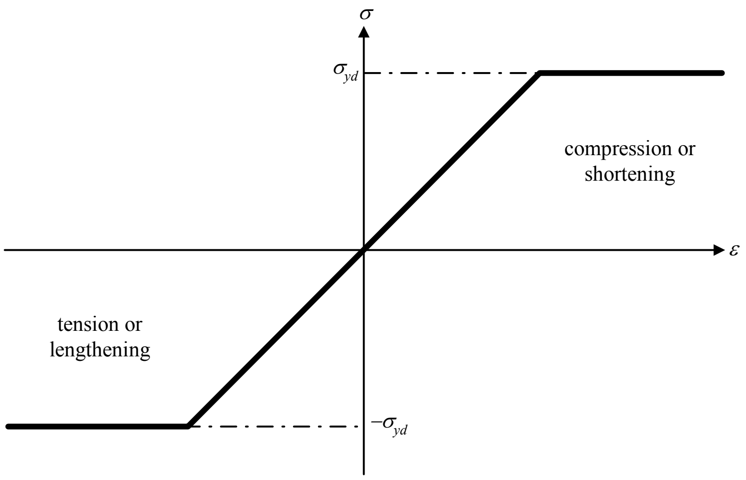
ε_E = element strain

ε_{yd} = strain corresponding to yield stress in the element

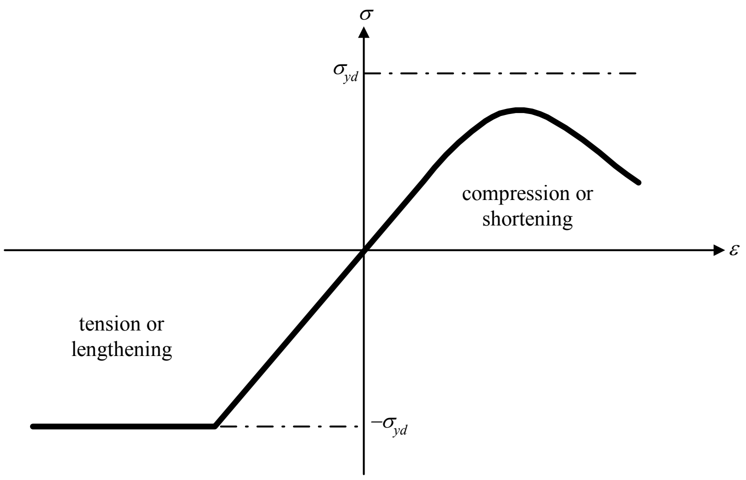
= $\dfrac{\sigma_{yd}}{E}$

FIGURE 4
Example of Stress Strain Curves σ-ε

a) Stress strain curve σ-ε for elastic, perfectly plastic failure of a hard corner



b) Typical stress strain curve σ-ε for elasto-plastic failure of a stiffener



3.5.3 Beam Column Buckling

The equation describing the shortening portion of the stress strain curve σ_{CR1}-ε for the beam column buckling of stiffeners is to be obtained from the following formula:

$$\sigma_{CR1} = \Phi\sigma_{C1}\left(\frac{A_s + b_{eff-p}t_p}{A_s + st_p}\right) \quad \text{N/cm}^2\ (\text{kg/cm}^2,\ \text{lbf/in}^2)$$

where

σ_{C1} = critical stress, N/cm^2 (kg/cm^2, lbf/in^2)

$= \dfrac{\sigma_{E1}}{\varepsilon}$ for $\sigma_{E1} \le \dfrac{\sigma_{yd}}{2}\varepsilon$

$= \sigma_{yd}\left(1 - \dfrac{\sigma_{yd}\varepsilon}{4\sigma_{E1}}\right)$ for $\sigma_{E1} > \dfrac{\sigma_{yd}}{2}\varepsilon$

σ_{E1} = Euler column buckling stress, N/cm^2 (kg/cm^2, lbf/in^2)

$$= \pi^2 E \frac{I_E}{A_E \ell^2}$$

ℓ = unsupported span of the longitudinal, cm (in.)

s = plate breadth taken as the spacing between the stiffeners, cm (in.)

I_E = net moment of inertia of stiffeners, cm^4 (in^4), with attached plating of width $b_{eff\text{-}s}$

$b_{eff\text{-}s}$ = effective width, cm (in.), of the attached plating for the stiffener

$= \dfrac{s}{\beta_p}$ for $\beta_p > 1.0$

$= s$ for $\beta_p \le 1.0$

$$\beta_p = \frac{s}{t_p}\sqrt{\frac{\varepsilon\sigma_{yd}}{E}}$$

A_E = net area of stiffeners, cm^2 (in^2), with attached plating of width $b_{eff\text{-}p}$

$b_{eff\text{-}p}$ = effective width of the plating, cm (in.)

$= \left(\dfrac{2.25}{\beta_p} - \dfrac{1.25}{\beta_p^2}\right)s$ for $\beta_p > 1.25$

$= s$ for $\beta_p \le 1.25$

3.5.4 Torsional Buckling of Stiffeners

The equation describing the shortening portion of the stress-strain curve σ_{CR2}-ε for the lateral-flexural buckling of stiffeners is to be obtained according to the following formula:

$$\sigma_{CR2} = \Phi\left(\frac{A_s\sigma_{C2} + st_p\sigma_{CP}}{A_s + st_p}\right) \quad \text{N/cm}^2 \text{ (kg/cm}^2\text{, lbf/in}^2\text{)}$$

where

σ_{C2} = critical stress, N/cm^2 (kg/cm^2, lbf/in^2)

$= \dfrac{\sigma_{E2}}{\varepsilon}$ for $\sigma_{E2} \le \dfrac{\sigma_{yd}}{2}\varepsilon$

$= \sigma_{yd}\left(1 - \dfrac{\sigma_{yd}\varepsilon}{4\sigma_{E2}}\right)$ for $\sigma_{E2} > \dfrac{\sigma_{yd}}{2}\varepsilon$

σ_{CP} = ultimate strength, N/cm^2 (kg/cm^2, lbf/in^2), of the attached plating for the stiffener

$= \left(\dfrac{2.25}{\beta_p} - \dfrac{1.25}{\beta_p^2}\right)\sigma_{yd}$ for $\beta_p > 1.25$

$= \sigma_{yd}$ for $\beta_p \le 1.25$

β_p = coefficient defined in 7/3.6.3

σ_{E2} = Euler torsional buckling stress, equal to reference stress for torsional buckling σ_{ET}, N/cm^2 (kg/cm^2, lbf/in^2)

σ_{ET} = $E[K/2.6 + (n\pi/\ell)^2\Gamma + C_o(\ell/n\pi)^2/E]/I_o[1 + C_o(\ell/n\pi)^2/I_o f_{cL}]$

K = St. Venant torsion constant for the longitudinal's cross section, excluding the associated plating

= $\left[b_f t_f^3 + d_w t_w^3\right]/3$, cm^3 (in^3)

I_o = polar moment of inertia of the longitudinal, excluding the associated plating, about the toe (intersection of web and plating)

= $I_x + mI_y + A_s\left(x_o^2 + y_o^2\right)$, cm^4 (in^4)

I_x, I_y = moment of inertia of the longitudinal about the x- and y-axis, respectively, through the centroid of the longitudinal, excluding the plating (x-axis perpendicular to the web)

m = $1.0 - u(0.7 - 0.1d_w/b_f)$

u = unsymmetry factor

= $1 - 2b_1/b_f$

C_o = $E t_p^3/3s$

Γ = warping constant

$\cong$ $mI_{yf}\, d_w^2 + d_w^3\, t_w^3/36$

I_{yf} = $t_f\, b_f^3\,(1.0 + 3.0\, u^2 d_w t_w/A_s)/12$

f_{cL} = critical buckling stress for the associated plating, corresponding to n-half waves

= $\pi^2 E(n/\alpha + \alpha/n)^2(t_p/s)^2/12(1 - \nu^2)$, N/cm^2 (kg/cm^2, lbf/in^2)

α = ℓ/s

ℓ = unsupported span of the longitudinal, cm (in.)

s = plate breadth taken as the spacing between the stiffeners, cm (in.)

n = number of half-wave which yield a smallest σ_{ET}

3.5.5 Web Local Buckling of Stiffeners with Flanged Profiles

The equation describing the shortening portion of the stress strain curve σ_{CR3}-ε for the web local buckling of flanged stiffeners is to be obtained from the following formula:

$$\sigma_{CR3} = \Phi\sigma_{yd}\left(\frac{b_{eff-p}t_p + d_{w-eff}t_w + b_f t_f}{st_p + d_w t_w + b_f t_f}\right) \quad \text{N/cm}^2\ (\text{kg/cm}^2,\ \text{lbf/in}^2)$$

where

s = plate breadth taken as the spacing between the stiffeners, cm (in.)

$b_{eff\text{-}p}$ = effective width of the attached plating, cm, defined in 7/3.6.3

$d_{w\text{-}eff}$ = effective depth of the web, cm (in.)

= $\left(\dfrac{2.25}{\beta_w} - \dfrac{1.25}{\beta_w^2}\right)d_w$ for $\beta_w > 1.25$

= d_w for $\beta_w \le 1.25$

β_w = $\dfrac{d_w}{t_w}\sqrt{\dfrac{\varepsilon\sigma_{yd}}{E}}$

3.5.6 Local Buckling of Flat Bar Stiffeners

The equation describing the shortening portion of the stress-strain curve σ_{CR4}-ε for the web local buckling of flat bar stiffeners is to be obtained from the following formula:

$$\sigma_{CR4} = \Phi\left(\frac{A_s\sigma_{C4} + st_p\sigma_{CP}}{A_s + st_p}\right) \quad \text{N/cm}^2 \text{ (kg/cm}^2\text{, lbf/in}^2\text{)}$$

where

σ_{CP} = ultimate strength of the attached plating, N/cm^2 (kg/cm^2, lbf/in^2)

σ_{C4} = critical stress, N/cm^2 (kg/cm^2, lbf/in^2)

= $\dfrac{\sigma_{E4}}{\varepsilon}$ for $\sigma_{E4} \le \dfrac{\sigma_{yd}}{2}\varepsilon$

= $\sigma_{yd}\left(1 - \dfrac{\sigma_{yd}\varepsilon}{4\sigma_{E4}}\right)$ for $\sigma_{E4} > \dfrac{\sigma_{yd}}{2}\varepsilon$

σ_{E4} = Euler buckling stress, N/cm^2 (kg/cm^2, lbf/in^2)

= $\dfrac{0.44\pi^2 E}{12(1-\nu^2)}\left(\dfrac{t_w}{d_w}\right)^2$

3.5.7 Buckling of Transversely Stiffened Plate Panels

The equation describing the shortening portion of the stress-strain curve σ_{CR5}-ε for the buckling of transversely stiffened panels is to be obtained from the following formula:

$$\sigma_{CR5} = \min\begin{cases}\sigma_{yd}\left[\dfrac{s}{\ell_{stf}}\left(\dfrac{2.25}{\beta_p} - \dfrac{1.25}{\beta_p^2}\right) + 0.115\left(1 - \dfrac{s}{\ell_{stf}}\right)\left(1 + \dfrac{1}{\beta_p^2}\right)^2\right] \\ \sigma_{yd}\Phi\end{cases} \quad \text{N/cm}^2 \text{ (kg/cm}^2\text{, lbf/in}^2\text{)}$$

where

β_p = coefficient defined in 7/3.6.3

s = plate breadth taken as the spacing between the stiffeners, cm (in.)

ℓ_{stf} = span of stiffener equal to spacing between primary support members, cm (in.)

SECTION 8 Fatigue Damage Assessment

1 General

This Section provides procedures for the fatigue assessment of container carriers including whipping. It is assumed that the damage is mainly due to vertical bending moment. It is also assumed that whipping occurs mainly in a bow seas wave environment.

The main objective of the assessment is to obtain the whipping contribution to the fatigue damage relative to that from normal wave loads. The contribution as a factor of wave-induced damage can be used in the detailed fatigue assessment, such as in the ABS *Guide for Application of Higher-Strength Hull Structural Thick Steel Plates in Container Carriers*.

The fatigue damage assessment, in general, includes the following three major steps:

- Prediction of fatigue damage without whipping
- Prediction of fatigue damage including whipping
- Whipping contribution to the fatigue damage

2 Fatigue Damage

2.1 General

For a single one-segment linear S-N curve, the closed form expression of damage, D can be calculated as follows:

$$D = \frac{T}{K}\left(2\sqrt{2}\right)^m \Gamma\left(\frac{m}{2}+1\right)\sum_i \lambda\left(m, \varepsilon_1\right) f_{0i}\, p_i \left(\sigma_i\right)^m$$

where

T	=	total target fatigue life, year
K, m	=	physical parameters describing the S-N curve
f_{0i}	=	zero up-crossing frequency of the stress amplitude at the i-th sea state, rad/s
p_i	=	probability of the i-th sea state in the wave scatter diagram
λ	=	wide-band correction factor
σ_i	=	standard deviation of the stress amplitude at the i-th sea state, N/cm^2 (kg/cm^2, lbf/in^2)
	=	S_M/f_s
f_s	=	stress coefficient. It can be approximated by a section modulus.
S_M	=	standard deviation of vertical bending moment, kN-m (tf-m, Ltf-ft)

This formula for the damage calculation is mainly based on the narrow banded Gaussian process and Palmgren-Miner rule. For a wide-banded stress process, a correction factor may be introduced. The following two Paragraphs provide the correction factors for wave frequency response and combined wave frequency and high frequency (whipping) response.

2.2 Wave-Frequency Response Fatigue Damage

The zero up-crossing frequency of the stress due to wave frequency loads can be calculated by:

$$f_0 = \frac{1}{2\pi}\frac{\sigma_2}{\sigma_0}$$

where σ_0 and σ_2 are the zero and second spectral moment of the stress response, respectively, and can be written as:

$$\sigma_n^2 = \int_0^\infty \omega^n S(\omega) d\omega$$

where $S(\omega)$ is the stress spectral distribution function.

The wide-band correction factor can be calculated by:

$$\lambda(m, \varepsilon_i) = a(m) + [1 - a(m)][1 - \varepsilon_i]^{b(m)}$$

where

$a(m) = 0.926 - 0.033m$

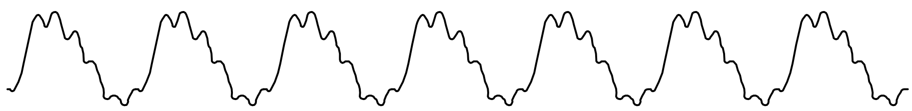
$b(m) = 1.587m - 2.323$

$$\varepsilon = \sqrt{1 - \frac{\sigma_2^4}{\sigma_0^2 \sigma_4^2}}$$

2.3 Combined Wave and Whipping Response Fatigue Damage

The stress response including wave frequency and whipping is a combined stationary and transient process. The stress response due to the wave frequency component can be assumed as a narrow-banded Gaussian process and the stress due to whipping as a time decaying transient process of high frequency. Section 8, Figure 1, depicts a combined stationery wave component stress and time decaying whipping stress.

FIGURE 1
Combination of a LF Stationary and HF Transient Process



The fatigue damage is estimated as the sum of the damage due to the envelope process (wave plus whipping response) and the high frequency transient whipping response alone. The mean damage of one transient process (one whipping event) can be calculated by:

$$D_{H,whipping} = \frac{T}{K}\left(2\sqrt{2}\right)^m \Gamma\left(\frac{m}{2}+1\right)\sum_i f_{0i}\, p_i \frac{\sigma_{i,whipping}^m}{1 - \exp(-2\pi\xi m)}$$

where

$\sigma_{i,whipping}$ = standard deviation of the stress amplitude due to whipping at the i-th sea state, N/cm^2 (kg/cm^2, lbf/in^2)

ξ = structural damping

Other parameters are as defined in 8/2.1.

The fatigue damage for the envelope process can be calculated by:

$$D_{ev} = (1 - \eta_{whipping})\frac{T}{K}\left(2\sqrt{2}\right)^m \Gamma\left(\frac{m}{2}+1\right)\sum_i f_{0i}\, p_i \sigma_{i,wave}^m \; + $$
$$\eta_{whipping}\frac{T}{K}\left(2\sqrt{2}\right)^m \Gamma\left(\frac{m}{2}+1\right)\sum_i f_{0i}\, p_i \left(\sigma_{i,wave} + \sigma_{i,whipping}\right)^m$$

where

$\eta_{whipping}$ = $\exp\left\{-\frac{1}{2}\left(\frac{V_{cr}}{S_{\dot{z}}}\right)^2\right\}$ probability of whipping occurrence

V_{cr} = threshold velocity , m/sec (ft/sec)

= $0.093\sqrt{Lg}$

L = vessel length, m (ft)

g = acceleration of gravity, m/s^2 (ft/s^2)

The total fatigue damage including whipping can be calculated as the sum the high frequency damage and the damage due to the envelope process as:

$$D = D_{H,whipping} + D_{ev}$$

2.4 Whipping Contribution to Fatigue Damage

Whipping fatigue damage contribution can be evaluated by the ratio of whipping-induced damage to the damage by wave frequency load as:

$$\alpha_w = 1 + \frac{D_{total} - D_{wave}}{D_{wave}}$$

where

D_{total} = total fatigue damage including whipping

D_{wave} = fatigue damage due to wave frequency load

α_w = fatigue damage factor including whipping

3 Fatigue Damage Assessment

As mentioned above, the objective of the fatigue assessment is to obtain the relative contribution to the fatigue damage due to whipping. The result can be used in the total fatigue damage where it is applicable, such as in the ABS *Guide for Application of Higher-Strength Hull Structural Thick Steel Plates in Container Carriers*. In the aforementioned Guide, the cumulative fatigue damage, D_f, is to be taken as:

$$D_f = \frac{1}{6}\alpha_s\alpha_w(D_{f\_12} + D_{f\_34}) + \frac{1}{3}D_{f\_56} + \frac{1}{3}D_{f\_78}$$

where

α_w = fatigue damage factor including whipping

α_s = fatigue damage factor including springing

D_{f_12}, D_{f_34}, D_{f_56}, and D_{f_78} are the fatigue damage accumulated due to load case pairs 1 & 2, 3 & 4, 5 & 6 and 7 & 8, respectively. The detailed procedures for the calculation for D_{f_12}, D_{f_34}, D_{f_56}, and D_{f_78} are given in Appendix 2.

APPENDIX 1 **Wave-Induced Vertical Bending Moment – A Simplified Method**

1 Wave Induced Bending Moment (Linear)

The frequency response function, Φ_M, for wave-induced vertical bending moment for a homogeneously loaded box-shape vessel can be derived analytically using the linear strip theory as.

$$\Phi_M \quad = \quad \frac{e^{-2\varsigma\tau}}{4\varsigma^2}\left(1-\cos\varsigma-\frac{\varsigma}{2}\sin\varsigma\right)(1-2\varsigma\tau)\rho g B L^2 \,|\cos\beta|^{1/3}$$

where

β = wave heading, 180 degree for head sea

ς = $\pi\Omega^2$

Ω = $\dfrac{\omega}{\sqrt{\dfrac{2\pi g}{L\,|\cos\beta|}}}$

τ = $\dfrac{d}{L}$

d = draft, m (ft)

L = vessel length, m (ft)

ω = wave frequency, rad/s

Correction factors for speed, V, and block coefficient, C_b, are introduced for vessels with forward speeds and with a small block coefficient. The standard deviation of wave induced bending moment can be written as:

$$s_M^2 = \left[F_v(F_n)F_{Cb}(C_b)\right]^2\int_0^\infty \Phi_M^2(\omega)S(\omega)d\omega$$

where

$F_v(F_n)$ = speed dependence factor

= $1 + 3F_n^2$ where $F_n < 0.3$

F_n = $\dfrac{V}{\sqrt{gL}}$, Froude Number

$F_{Cb}(C_b)$ = $[(1-\theta)^2 + 0.6\theta(2-\theta)]$

θ = $2.5(1 - C_b)$

C_b = $\max(0.6, C_b)$

ω = wave frequency, rad/s

$S(\omega)$ is the wave spectrum model formulated in the wave frequency, ω, the significant wave heights, H_s, and the zero-up-crossing period T_z:

$$S(\omega) = 4\, H_s^2\, \pi^3 T_z (\omega T_z)^{-5}\, e^{-\pi^3 (\omega T_z / 2)^{-4}}$$

Assuming that the wave-induced response is a Gaussian stochastic process with zero mean and the spectral density function, $S_y(\omega)$, is narrow banded, the probability density function of the maxima (peak values) may be represented by a Rayleigh distribution. Then, the short-term probability of the response exceeding x_0, $P\{x > x_0\}$, for the j-th sea state may be expressed by the following equation:

$$P_j\{x > x_0\} = \exp\left(-\frac{x_0^2}{2 S_{Mj}^2} \right)$$

2 Wave Induced Bending Moment (Non-Linear)

Non-linear sagging bending moment increases with bow flare, while non-linear hogging bending moment is typically slightly lower than the linear prediction. This Subsection provides a simplified method for the prediction of statistical value of sagging bending moment.

For a non-linear sagging bending moment, the probability, P, that an individual peak, M, exceeds a given level, m, can be calculated by:

$$P(M > m) = \exp\left(-\frac{1}{2} u^2(m) \right)$$

where

$$u = \frac{-1 + \sqrt{1 + 4\chi(\chi + \overline{m}}}{2\chi}$$

$$\chi = \frac{\kappa_3}{6}$$

$$\overline{m} = \frac{m}{S_m r}$$

$$r = \left(1 + \frac{\kappa_3^2}{18} \right)^{-\frac{1}{2}}$$

S_m = standard deviation of wave-induced bending moment given in Subsection A1/1

κ_3 = skewness of the bending moment. It can be approximated by an analytical expression in the wave spectrum parameters H_s, T_z, the bow flare coefficient C_f and the Froude number F_n:

$$= 0.26 H_s C_f \left(1 - \exp\left(\frac{-10 F_n}{|\cos\alpha|} \right) \right) \min\left(\frac{T_z - 5}{5}, 1 \right)$$

C_f = difference between the upper deck area and the water plane area in the forward 20% of the vessel's hull normalized the freeboard in this area and with the vessel length.

APPENDIX 2 Fatigue Damage Assessment

1 General

1.1 Note

The criteria in 5C-5-A1 of the ABS *Rules for Building and Classing Steel Vessels (Steel Vessel Rules)* provide a design oriented approach to fatigue strength assessment which may be used, for certain structural details, in lieu of more elaborate methods such as spectral fatigue analysis. This Appendix offers specific guidance on a full ship finite element based fatigue strength assessment of certain structural details in the upper flange of container carrier hull structure. The term “assessment” is used here to distinguish this approach from the more elaborate analysis.

Under the design torsional moment curves defined in 5C-5-3/5.1.5 of the *Steel Vessel Rules*, the warping stress distributions can be accurately determined from a full ship finite element model for novel container carrier configurations, for example:

- Engine room and deckhouse co-located amidships
- Engine room and deckhouse that are separately located
- Fuel oil tanks located within cargo tanks

The full ship finite element based fatigue strength assessment is considered an essential step in evaluating hull structural thick steel plates in large container carriers.

The criteria in this Appendix are developed from various sources. including the Palmgren-Miner linear damage model, S-N curve methodologies, long-term environment data of the North-Atlantic Ocean, etc., and assume workmanship of commercial marine quality acceptable to the Surveyor.

1.2 Applicability

The criteria in this Appendix are specifically written for container carriers to which Part 5C, Chapter 5 of the *Steel Vessel Rules* is applicable.

1.3 Loadings

The criteria have been written for ordinary wave-induced motions and loads. Other cyclic loadings, which may result in significant levels of stress ranges over the expected lifetime of the vessel, are also to be considered by the designer.

Where it is known that a vessel will be engaged in long-term service on a route with a more severe environment, the fatigue strength assessment criteria in this Guide are to be modified accordingly.

1.4 Effects of Corrosion

To account for the mean wastage throughout the service life, the total stress range calculated from a full ship finite element model using the gross scantlings is modified by a factor c_f. See A2/5.2.1.

1.5 Format of the Criteria

The criteria in this Appendix are presented as a comparison of fatigue strength of the structure (capacity) and fatigue inducing loads (demands) as represented by the calculated cumulative fatigue damage over the design service life of 20 years in the North Atlantic Ocean. In other words, the calculated cumulative fatigue damage is to be not less than 0.8.

2 Connections to be Considered for the Fatigue Strength Assessment

2.1 General

The criteria in this Appendix have been developed to allow consideration of a broad variation of structural details and arrangements in the upper flange of a container carrier hull structure so that most of the important structural details anywhere in the vessel can be subjected to an explicit (numerical) fatigue assessment using these criteria. However, where justified by comparison with details proven satisfactory under equal or more severe conditions, an explicit assessment can be exempted.

2.2 Guidance on Locations

As a general guidance for assessing fatigue strength for a container carrier, the following connections and locations are to be considered:

2.2.1 Hatch Corners

The following locations of hatch corners:

2.2.1(a) Typical hatch corners within 0.4*L* amidships

2.2.1(b) Hatch corners at the forward cargo hold

2.2.1(c) Hatch corners immediately forward and aft of the engine room

2.2.1(d) Hatch corners immediately forward and aft of the accommodation block, if it is not co-located with the engine room

2.2.1(e) Hatch corners subject to significant warping constraint from the adjacent structures

2.2.2 Connection of Longitudinal Hatch Girders and Cross Deck Box Beams to Other Supporting Structures

Representative locations of each hatch girder and cross deck box beam connection.

2.2.3 Representative Cut-outs

Representative cut-outs in the longitudinal bulkheads, longitudinal deck girder, hatch side coamings, and cross deck box beams.

2.2.4 Other Regions and Locations

Other regions and locations highly stressed by fluctuating loads, as identified from the full ship finite element torsional analysis.

For the structural details identified above, the stress concentration factor (SCF) may be calculated by the approximate equations given in Subsection A2/5. Alternatively, the stress concentration factor (SCF) may be determined from fine mesh F.E.M. analyses (see Subsection A2/6).

2.3 Fatigue Classification

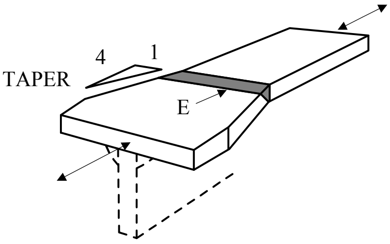
2.3.1 Welded Connections with One Load Carrying Member

Fatigue classification for structural details is shown in Appendix 2, Table 1.

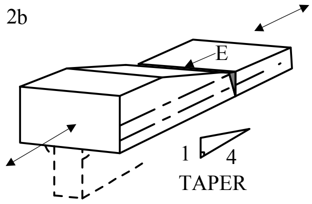
TABLE 1
Fatigue Classification for Structural Details

Class Designation	*Description*
B	Parent materials, plates or shapes as-rolled or drawn, with no flame-cut edges. In case with any flame-cut edges, the flame-cut edges are subsequently ground or machined to remove all visible sign of the drag lines
C	1) Parent material with automatic flame-cut edges 2) Full penetration seam welds or longitudinal fillet welds made by an automatic submerged or open arc process, and with no stop-start positions within the length.
D	1) Full penetration butt welds made either manually or by an automatic process other than submerged arc, from both sides, in downhand position. 2) Weld in C-2) with stop-start positions within the length
E	1) Full penetration butt welds made by other processes than those specified under D-1) 2) Full penetration butt welds made from both sides between plates of unequal widths or thicknesses

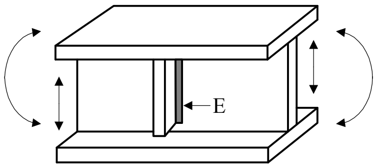
2a



2b



3) Welds of brackets and stiffeners to web plate of girders



F	1) Full penetration butt weld made on a permanent backing strip 2) Rounded fillet welds as shown below

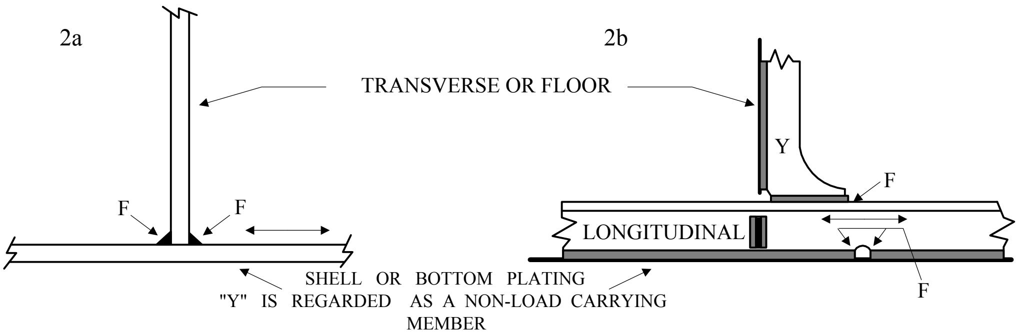
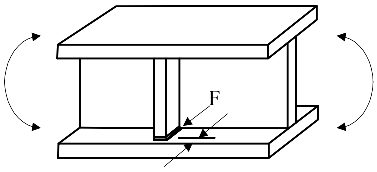
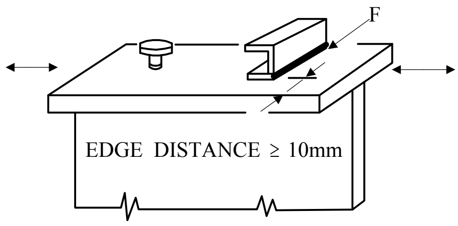


TABLE 1 (continued)
Fatigue Classification for Structural Details

Class Designation	*Description*
	3) Welds of brackets and stiffeners to flanges



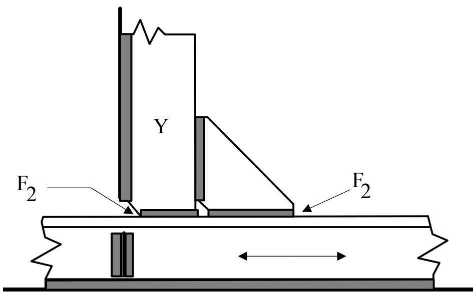
4) Attachments on plate or face plate



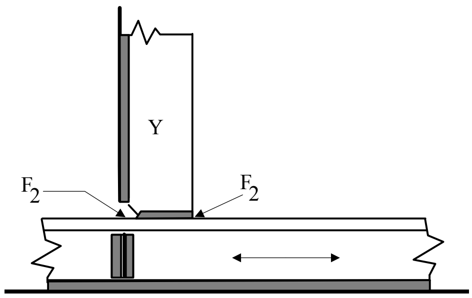
(Class G for edge distance < 10 mm)

F_2 1) Fillet welds as shown below with rounded welds and no undercutting

1a



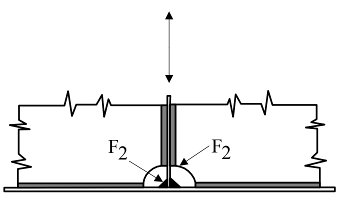
1b



"Y" is a non-load carrying member

2) Fillet welds with any undercutting at the corners dressed out by local grinding

2a)



2b)

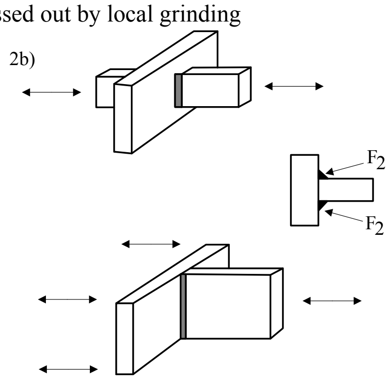
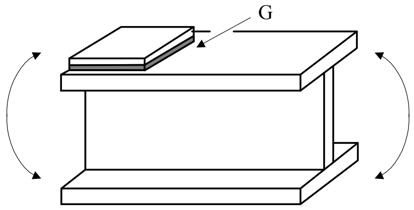
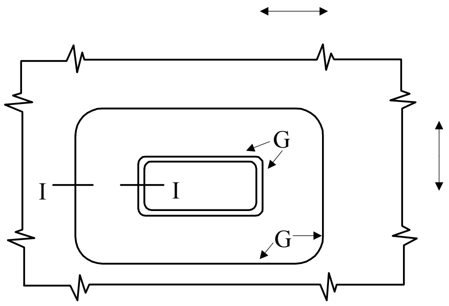
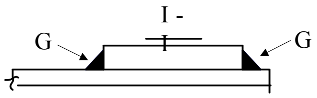


TABLE 1 (continued)
Fatigue Classification for Structural Details

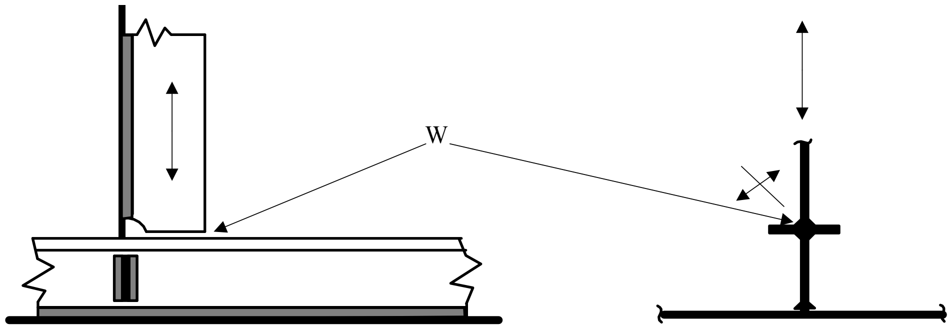
Class Designation	*Description*
G	1) Fillet welds in F_2 - 1) without rounded toe welds or with limited minor undercutting at corners or bracket toes 2) Fillet welds in F_2 - 2) with minor undercutting 3) Doubler on face plate or flange, small deck openings 4) Overlapped joints as shown below







W	1) Fillet welds in G - 3) with any undercutting at the toes 2) Fillet welds – weld throat

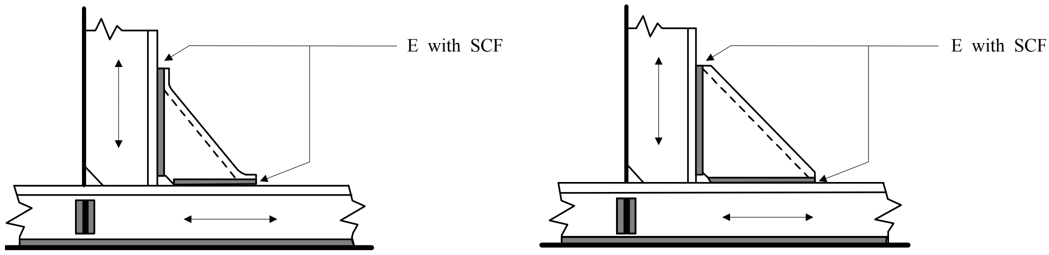


2.3.2 Welded Joint with Two or More Load Carrying Members

For brackets connecting two or more load carrying members, an appropriate stress concentration factor (SCF) determined from fine mesh finite element analysis is to be used. In this connection, the fatigue class at bracket toes may be upgraded to class E. Sample connections are illustrated below with/without SCF.

TABLE 2
Welded Joint with Two or More Load Carrying Members

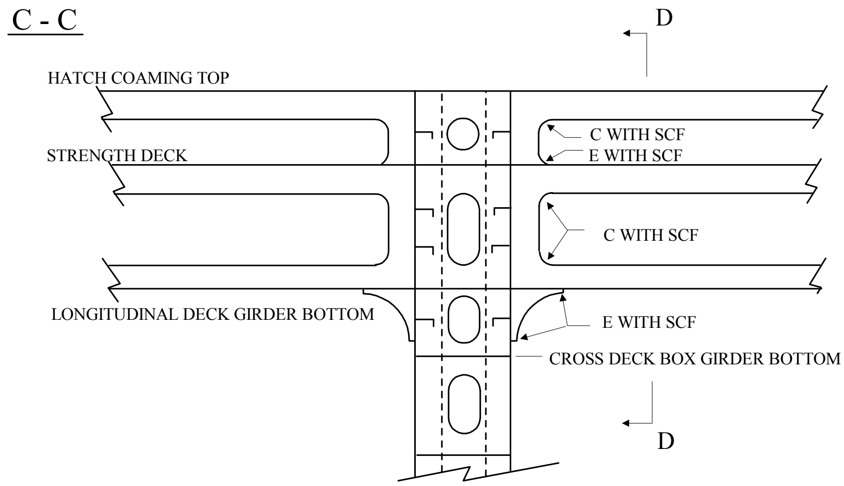
a Connections of Longitudinal and Stiffener



b Connections of Longitudinal Deck Girders and Cross Deck Box

c

TABLE 2 (continued)
Welded Joint with Two or More Load Carrying Members



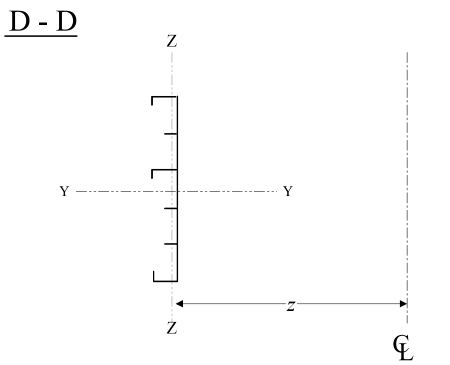
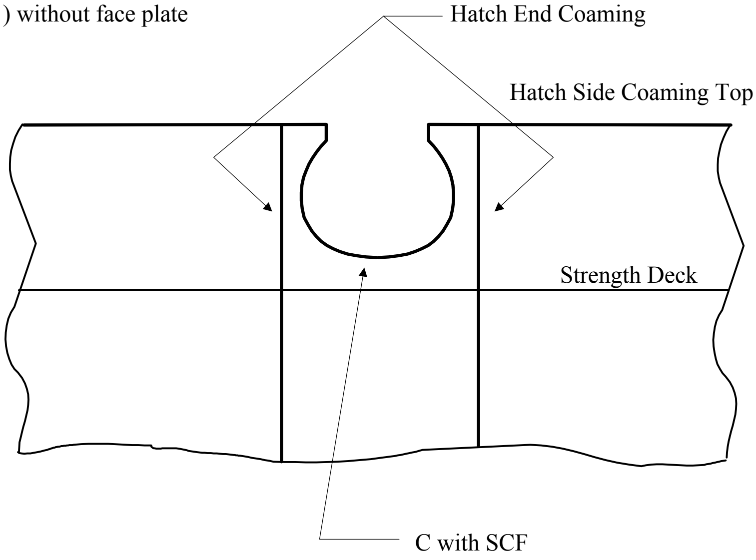


TABLE 2 (continued)
Welded Joint with Two or More Load Carrying Members

c Discontinuous Hatch Side Coaming

1) without face plate



2) with face plate

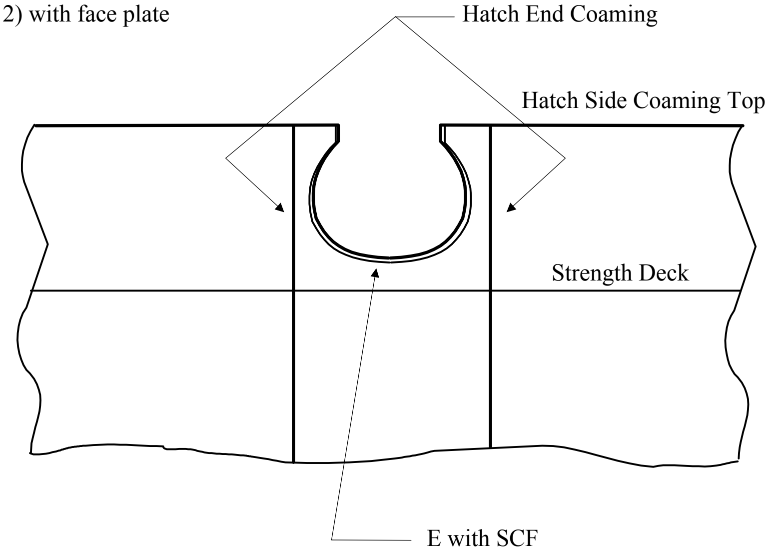
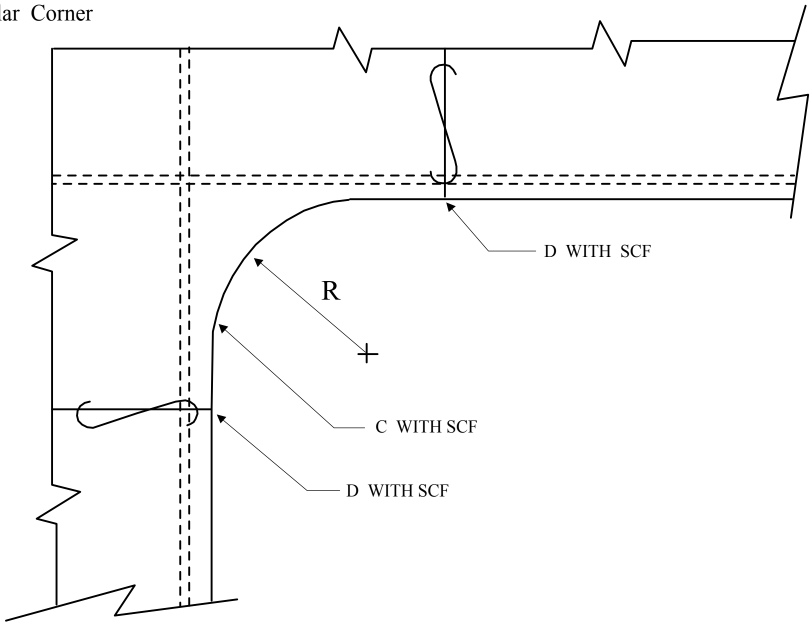


TABLE 2 (continued)
Welded Joint with Two or More Load Carrying Members

d Hatch Corners

Circular Corner



Double Curvature

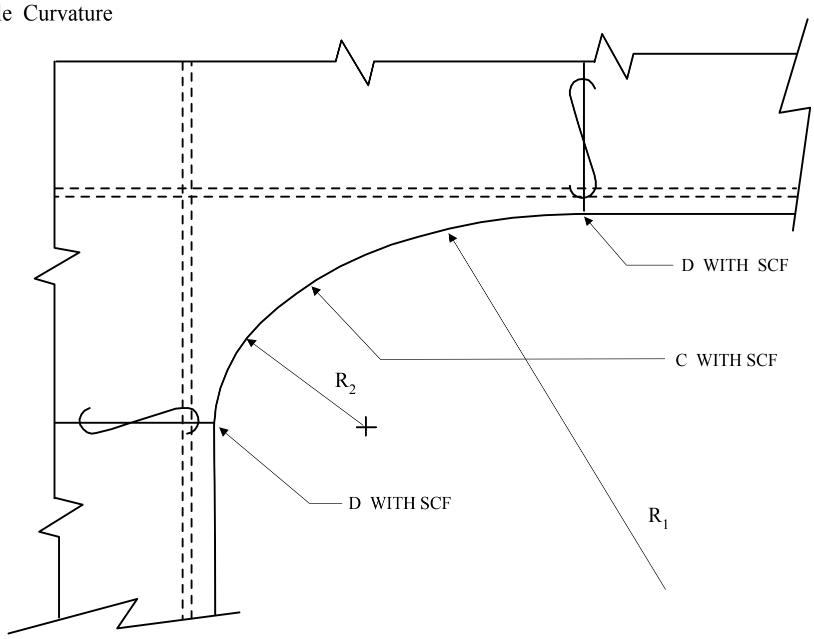


TABLE 2 (continued)
Welded Joint with Two or More Load Carrying Members

Cut-out Radius

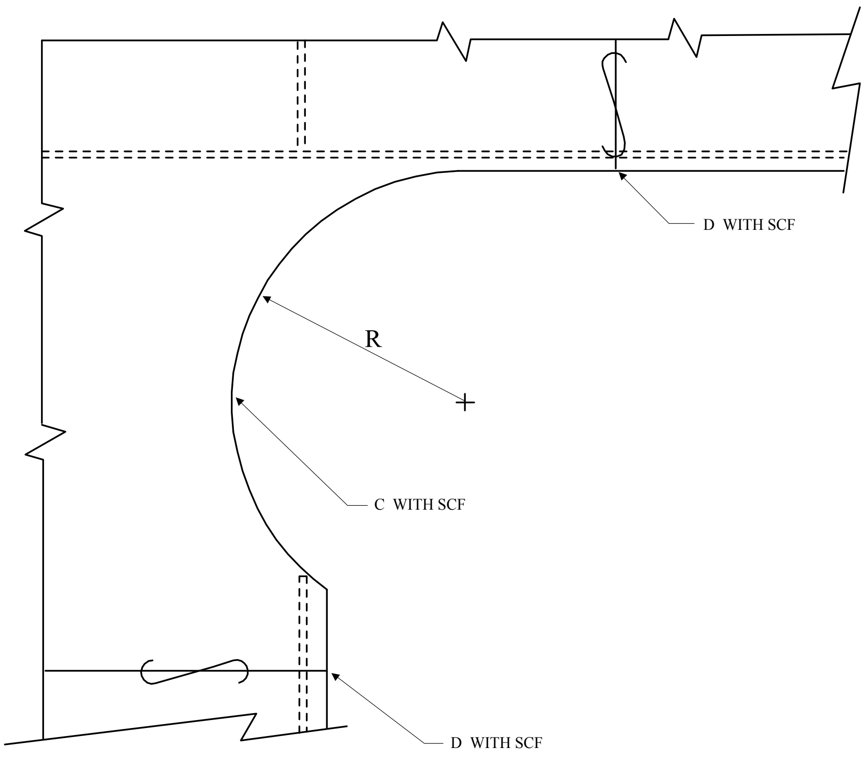
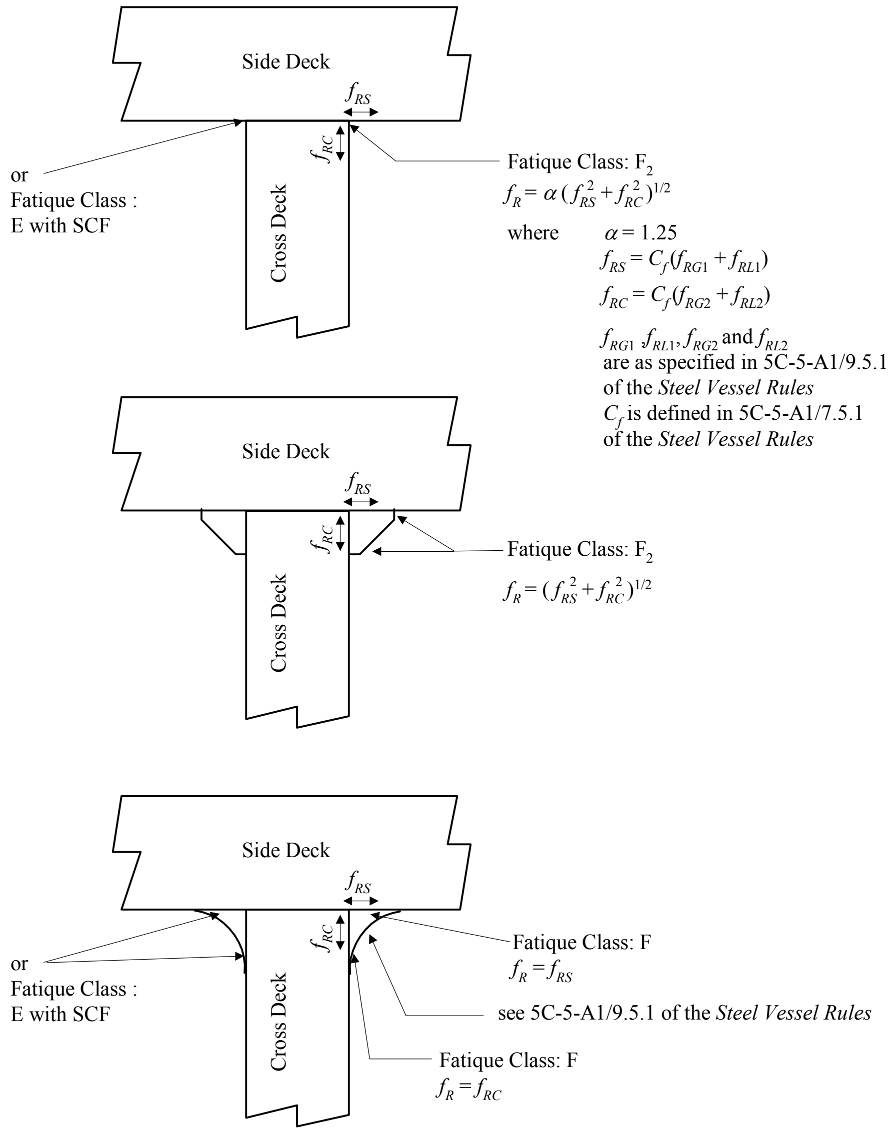


TABLE 2 (continued)
Welded Joint with Two or More Load Carrying Members

End Connections at Lower Deck



Note: Thickness of brackets is to be not less than that of cross deck plating in the same location (level).

For fitting of cell guide, no cut nor welding to the brackets is allowed.

3 Fatigue Damage Calculation

3.1 Assumptions

The fatigue damage of a structural detail under the loads specified here is to be evaluated using the criteria contained in this Subsection. The key assumptions employed are listed below for guidance.

- A linear cumulative damage model (i.e., Palmgren-Miner's Rule) has been used in connection with the S-N data in Appendix 2, Figure 1 (extracted from Ref. 1*).

  * Ref. 1: "Offshore Installations: Guidance on Design, Construction and Certification", Department of Energy, U.K., Fourth Edition - 1990, London: HMSO

- Cyclic stresses due to the loads in Subsection A2/5 have been used, and the effects of mean stress have been ignored.
- The target design life of the vessel is taken to be 20 years.
- The long-term stress ranges on a detail can be characterized by using a modified Weibull probability distribution parameter (γ).
- Structural details are classified and described in Appendix 2, Table 1, "Fatigue Classification for Structural Details".

The structural detail classification in Appendix 2, Table 1 is based on joint geometry and direction of the dominant load. Where the loading or geometry is too complex for a simple classification, a finite element analysis of the details is to be carried out to determine the stress concentration factors. Subsection A2/6 contains guidance on finite element analysis modeling to determine stress concentration factors for weld toe locations that are typically found at longitudinal stiffener end connections.

3.2 Criteria

The fatigue damage, D_f, obtained using the criteria in A2/3.4, is to be not greater than 0.8.

3.3 Long Term Stress Distribution Parameter, γ

The long-term stress distribution parameter, γ, can be determined as shown below:

$$\gamma = \alpha\left(1.1 - 0.35\frac{L-100}{300}\right)$$

where

α = 1.0 for deck structures, including side shell and longitudinal bulkhead structures within $0.1D$ from the deck

= 1.05 for bottom structures, including inner bottom and side shell, and longitudinal bulkhead structures within $0.1D$ from the bottom

= 1.1 for side shell and longitudinal bulkhead structures within the region of $0.25D$ upward and $0.3D$ downward from the mid-depth

= 1.1 for transverse bulkhead structures

α may be linearly interpolated for side shell and longitudinal bulkhead structures between $0.1D$ and $0.25D$ from the deck, and between $0.1D$ and $0.2D$ from the bottom.

L = vessel's length, as defined in 3-1-1/3.1 of the *Steel Vessel Rules*.

D = vessel's depth, as defined in 3-1-1/7 of the *Steel Vessel Rules*.

3.4 Fatigue Damage

The cumulative fatigue damage, D_f, is to be taken as:

$$D_f = \frac{1}{6}\alpha_s\alpha_w(D_{f\_12} + D_{f\_34}) + \frac{1}{3}D_{f\_56} + \frac{1}{3}D_{f\_78} \le 0.8$$

where

α_s = fatigue damage factor due to hull girder springing. α_s is the ratio of the fatigue damage of a flexible hull girder and that of a rigid body hull girder due to wave-induced vertical bending moment in head or rear seas. If the effect of hull girder springing is ignored, α_s is equal to 1.0. For a flexible hull girder structure, α_s is greater than 1.0. α_s is to be determined based on well documented experimental data or analytical studies. When these direct calculations are not available, α_s may be conservatively taken as 1.3.

α_w = fatigue damage factor due to hull girder whipping. α_w is the ratio of the fatigue damage of a flexible hull girder and that of a rigid body hull girder due to wave-induced vertical bending moment in head or rear seas. If the effect of hull girder whipping is ignored, α_w is equal to 1.0. For a flexible hull girder structure, α_w is greater than 1.0. α_w is to be determined based on well documented experimental data or analytical studies. When these direct calculations are not available, α_w may be conservatively taken as 1.3.

D_{f_12}, D_{f_34}, D_{f_56} and D_{f_78} are the fatigue damage accumulated due to load case pairs 1 & 2, 3 & 4, 5 & 6 and 7 & 8, respectively (see Subsection A2/4 for load case pairs).

Assuming the long term distribution of stress ranges follow the Weibull distribution, the fatigue damage accumulated due to load pair *jk* is:

$$D_{f\_jk} = \frac{N_T}{K_2}\frac{\left(k_t k_h f_{R\_jk}\right)^m}{\left(\ln N_R\right)^{m/\gamma}}\mu_{jk}\Gamma\left(1+\frac{m}{\gamma}\right)$$

where

N_T = number of cycles in the design life

= $\dfrac{f_0 D_L}{4\log L}$

f_0 = 0.85, factor for net time at sea

D_L = design life in seconds, 6.31×10^8 for a design life of 20 years

L = vessel length defined in 3-1-1/3.1 of the *Steel Vessel Rules*

m, K_2 = S-N curve parameters, as defined in Appendix 2, Figure 1 of the Guide

f_{R_jk} = stress range of load case pair *jk* at the representative probability level of 10^{-4}, in N/cm^2 (kgf/cm^2, lbf/in^2)

k_t = thickness correction factor

= $\left(\dfrac{t}{22}\right)^n$ for $t \ge 22$ mm, where t is the plate thickness

= 1 for $t < 22$ mm

n = 0.20 for a transverse butt weld with its upper and lower edges as built or ground to 1C

= 0.10 for a transverse butt weld with its upper and lower edges ground with a radius of 3 ~ 5 mm. The extent of the grinding is to be 100 mm forward and aft of the butt weld as shown in Appendix 2, Table 1.

= 0.10 for hatch corner insert plate away from the welds. The upper and lower edges are ground with a radius of 3 ~ 5 mm

k_h = correction factor for higher-strength steel, applicable to parent material only

= 1.000 for mild steel or welded connections

= 0.926 for H32 steel

= 0.885 for H36 steel

= 0.870 for H40 steel

= 0.850 for H47 steel

N_R = 10000, number of cycles corresponding to the probability level of 10^{-4}

γ = long-term stress distribution parameter as defined in A2/3.3

Γ = Complete Gamma function

$$\mu_{jk} = 1 - \left\{ \frac{\Gamma_0\left(1+\frac{m}{\gamma}, \nu_{jk}\right) - \nu_{jk}^{-\Delta m/\gamma}\Gamma_0\left(1+\frac{m+\Delta m}{\gamma}, \nu_{jk}\right)}{\Gamma\left(1+\frac{m}{\gamma}\right)} \right\}$$

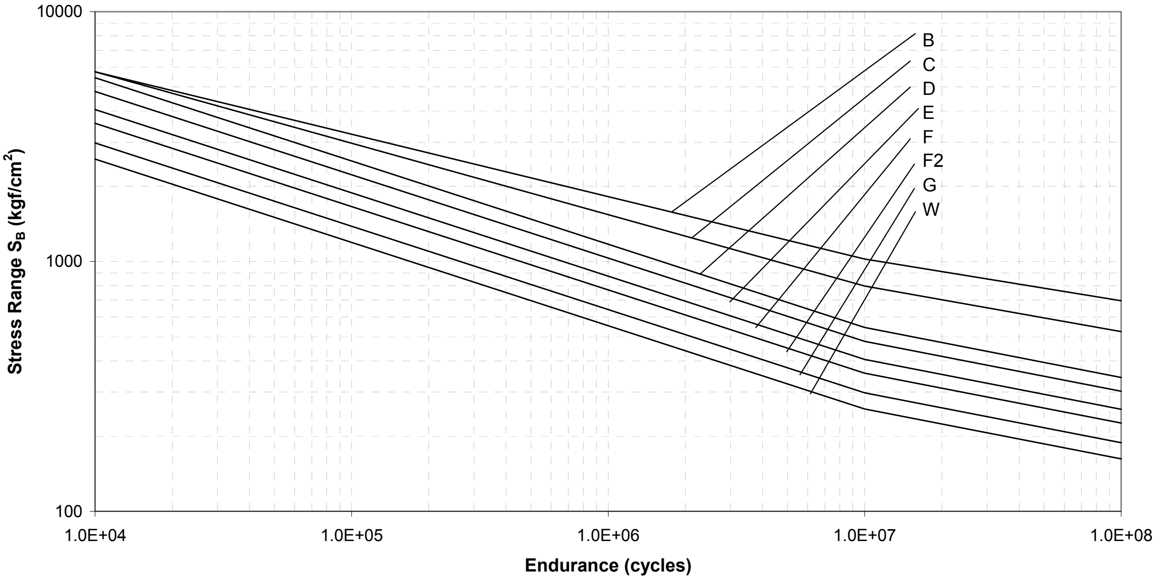
$$\nu_{jk} = \left(\frac{f_q}{f_{R\_jk}}\right)^{\gamma} \ln N_R$$

f_q = stress range at the intersection of the two segments of the S-N curve

Δm = 2, slope change of the upper-lower segment of the S-N curve

$\Gamma_0(\,)$ = incomplete Gamma function, Legendre form

FIGURE 1
Basic Design S-N Curves



Notes for Figure 1:

Basic design S-N curves

The basic design curves consist of linear relationships between $\log(S_B)$ and $\log(N)$. They are based upon a statistical analysis of appropriate experimental data and may be taken to represent two standard deviations below the mean line. Thus the basic S-N curves are of the form:

$$\log(N) = \log(K_2) - m \log(S_B)$$

where

$\log(K_2)$	=	$\log(K_1) - 2\sigma$
N	=	predicted number of cycles to failure under stress range S_B
K_1	=	a constant relating to the mean S-N curve
σ	=	standard deviation of $\log N$;
m	=	inverse slope of the S-N curve

The relevant values of these terms are shown in the table below and stress range is in kgf/cm^2. The S-N curves have a change of inverse slope from m to $m + 2$ at $N = 10^7$ cycles.

Class	K_1	$\log_{10} K_1$	m	σ	K_2	$\log_{10} K_2$
B	2.521×10^{19}	19.4016	4.0	0.1821	1.09×10^{19}	19.0374
C	3.660×10^{17}	17.5635	3.5	0.2041	1.43×10^{17}	17.1553
D	4.225×10^{15}	15.6258	3.0	0.2095	1.61×10^{15}	15.2068
E	3.493×10^{15}	15.5432	3.0	0.2509	1.10×10^{15}	15.0414
F	1.825×10^{15}	15.2614	3.0	0.2183	6.68×10^{14}	14.8248
F_2	1.302×10^{15}	15.1148	3.0	0.2279	4.56×10^{14}	14.6590
G	6.051×10^{14}	14.7818	3.0	0.1793	2.65×10^{14}	14.4232
W	3.978×10^{14}	14.5996	3.0	0.1846	1.70×10^{14}	14.2304

4 Fatigue Inducing Loads and Load Combination Cases

4.1 General

This Subsection provides: 1) the criteria to define the individual load components considered to cause fatigue damage in the upper flange of a container carrier hull structure (see A2/4.2); 2) the load combination cases to be considered for the upper flange of the hull structure containing the structural detail being evaluated (see A2/4.3).

4.2 Wave-induced Loads

The fluctuating load components to be considered are those induced by the seaway. They are divided into the following three groups:

- Hull girder wave-induced vertical bending moment
- Hull girder wave-induced horizontal bending moment
- Hull girder wave-induced torsional moment

4.3 Combinations of Load Cases for Fatigue Assessment

A container loading condition is considered in the calculation of stress range. For this loading condition, eight (8) load cases, as shown in Appendix 2, Table 3, are defined to form four (4) pairs. The combinations of load cases are to be used to find the characteristic stress range corresponding to a probability of exceedance of 10^{-4}, as indicated below.

TABLE 3
Combined Load Cases for Fatigue Strength Formulation

	L.C. 1	*L.C. 2*	*L.C. 3*	*L.C. 4*	*L.C. 5*	*L.C. 6*	*L.C. 7*	*L.C. 8*
Wave Induced Vertical Bending Moment	Sag 100%	Hog 100%	Sag 70%	Hog 70%	Sag 30%	Hog 30%	Sag 40%	Hog 40%
Wave Induced Horizontal Bending Moment	0.0	0.0	0.0	0.0	Stbd Tens 30%	Port Tens 30%	Stbd Tens 50%	Port Tens 50%
Wave Induced Torsional Moment	0.0	0.0	0.0	0.0	(–) 55%	(+) 55%	(–) 100%	(+) 100%
Wave Heading Angle	Head & Follow	Head & Follow	Head & Follow	Head & Follow	Beam	Beam	Oblique	Oblique

Notes:

1 Wave induced vertical bending moment is defined in 5C-5-3/5.1.1 of the *Steel Vessel Rules.*

2 Wave induced horizontal bending moment is defined in 5C-5-3/5.1.3 of the *Steel Vessel Rules.*

3 Wave induced torsional moment and sign convention are defined in 5C-5-3/5.1.5 of the *Steel Vessel Rules.*

4.3.1 Standard Load Combination Cases

4.3.1(a) Calculate dynamic component of stresses for load cases LC1 through LC8, respectively.

4.3.1(b) Calculate four sets of stress ranges, one each for the following four pairs of combined loading cases.

LC1 and LC2,

LC3 and LC4,

LC5 and LC6, and

LC7 and LC8

4.3.2 Vessels with Either Special Loading Patterns or Special Structural Configuration

For vessels with either special loading patterns or special structural configurations/features, additional load cases may be required for determining the stress range.

5 Determination of Wave-induced Stress Range

5.1 General

This Subsection contains information on the fatigue inducing stress range to be used in the fatigue assessment.

Where, for a particular example shown, no specific value of SCF is given when one is called for, it indicates that a finite element analysis is needed. When the fine mesh finite element approach is used, additional information on calculations of stress concentration factors and the selection of compatible S-N data is given in Subsection A2/6.

5.2 Hatch Corners

5.2.1 Hatch Corners at Decks and Coaming Top

The peak stress range, f_R, for hatch corners at the strength deck, the top of the continuous hatch side coaming, and the lower decks which are effective for the hull girder strength may be approximated by the following equation:

$$f_R = 0.5^{1/\gamma} \times c_f\,(K_{s1}c_{L1}f_{RG1} + K_{s2}c_{L2}f_{RG2}) \quad \text{N/cm}^2 \text{ (kgf/cm}^2\text{, lbf/in}^2\text{)}$$

where

f_{RG1} = global dynamic longitudinal stress range at the inboard edge of the strength deck, top of continuous hatch side coaming, and lower deck of hull girder section under consideration clear of hatch corner, in N/cm^2 (kgf/cm^2, lbf/in^2)

= $|f_{d1vi} - f_{d1vj}| + |f_{d1hi} - f_{d1hj}| + |f_{d1wi} - f_{d1wj}|$

f_{RG2} = bending stress range in connection with hull girder twist induced by torsion in cross deck structure in transverse the direction, in N/cm^2 (kgf/cm^2, lbf/in^2)

= $|f_{d1ci} - f_{d1cj}|$

c_f = adjustment factor to reflect a mean wasted condition

= 1.05

f_{d1vi}, f_{d1vj} = wave-induced component of the primary stresses produced by hull girder vertical bending, in N/cm^2 (kgf/cm^2, lbf/in^2), for load case i and j of the selected pairs of combined load cases, respectively. For this purpose, k_w is to be taken as $(1.09 + 0.029V - 0.47C_b)^{1/2}$ in calculating M_w (sagging and hogging) in 5C-5-3/5.1.1 of the *Steel Vessel Rules*

f_{d1hi}, f_{d1hj} = wave-induced component of the primary stresses produced by hull girder horizontal bending, in N/cm^2 (kgf/cm^2, lbf/in^2), for load case i and j of the selected pairs of combined load cases, respectively. See 5C-5-3/5.1.3 of the *Steel Vessel Rules*

f_{d1wi}, f_{d1wj} = wave-induced component of the primary stresses produced by hull girder torsion (warping stress) moment, in N/cm^2 (kgf/cm^2, lbf/in^2), for load case i and j of the selected pairs of combined load cases, respectively. See 5C-5-3/5.1.5 of the *Steel Vessel Rules*. The warping stress values in the longitudinal and transverse directions are to be taken at 1/8th of the 40-foot container bay length from the hatch opening corner.

For calculating the wave-induced stresses, sign convention is to be observed for the respective directions of wave-induced loads, as specified in Appendix 2, Table 3. These wave-induced stresses are to be determined based on the gross ship scantlings (A2/1.4).

f_{d1v} and f_{d1h} may be calculated by a simple beam approach. f_{d1w} in way of hatch corners at strength deck, top of continuous hatch side coaming, and lower deck may be determined from the full ship finite element model.

γ is as defined in A2/3.3.

K_{s1} and K_{s2} are stress concentration factors for the hatch corners considered and can be obtained by a direct finite element analysis. When a direct analysis is not available, these may be obtained from the following equations, but are not to be taken less than 1.0:

$$K_{s1} = c_t \alpha_{t1} \alpha_c \alpha_s k_{s1}$$

$$K_{s2} = \alpha_{ct} \alpha_{t2} k_{s2}$$

where

k_{s1} = nominal stress concentration factor in longitudinal direction, as given in the table below

k_{s2} = nominal stress concentration factor in transverse direction, as given in the table below

c_t = 0.8 for locations where coaming top terminates

= 1.0 for other locations

α_c = adjustment factor for cutout at hatch corners

= 1.0 for shapes without cutout

= $[1 - 0.04(c/R)^{3/2}]$ for circular shapes with a cutout

= $[1 - 0.04(c/r_d)^{3/2}]$ for double curvature shapes with a cutout

= $[1 - 0.04(c/R_1)^{3/2}]$ for elliptical shapes with a cutout

α_s = adjustment factor for contour curvature

= 1.0 for circular shapes

= $0.33[1 + 2(r_{s1}/r_d) + 0.1(r_d/r_{s1})^2]$ for double curvature shapes

= $0.33[1 + 2(R_2/R_1) + 0.1(R_1/R_2)^2]$ for elliptical shapes

α_{ct} = 1.0 for shapes without cutout

= 0.5 for shapes with cutout

α_{t1} = $(t_s/t_i)^{1/2}$

α_{t2} = $6.0/[5.0 + (t_i/t_c)]$, but not less than 0.85

α_{t1} or α_{t2} is to be taken as 1.0 where the longitudinal or transverse extent of the reinforced plate thickness in way of the hatch corner is less than that required in A2/5.2.3, as shown in Appendix 2, Figure 2.

r_{s1} = R for circular shapes in Appendix 2, Figure 3, in mm (in.)

= $[3R_1/(R_1 - R_2) + \cos\theta]r_{e2}/[3.816 + 2.879R_2/(R_1 - R_2)]$
for double curvature shapes in Appendix 2, Figure 4, in mm (in.)

= R_2 for elliptical shapes in Appendix 2, Figure 5, in mm (in.)

r_{s2} = R for circular shapes in Appendix 2, Figure 3, in mm (in.)

= R_2 for double curvature shapes in Appendix 2, Figure 4, in mm (in.)

= R_2^2/R_1 for elliptical shapes in Appendix 2, Figure 5, in mm (in.)

r_d = $(0.753 - 0.72R_2/R_1)[R_1/(R_1 - R_2) + \cos\theta]r_{e1}$

t_s = plate thickness of the strength deck, hatch side coaming top, or lower deck clear of the hatch corner under consideration, in mm (in.)

t_c = plate thickness of the cross deck, hatch end coaming top, or bottom of cross box beam clear of the hatch corner under consideration, in mm (in.)

t_i = plate thickness of the strength deck, hatch coaming top, or lower deck in way of the hatch corner under consideration, in mm (in.)

R, R_1, and R_2 for each shape are as shown in Appendix 2, Figures 3, 4 and 5.

θ for double curvature shapes is defined in Appendix 2, Figure 4.

r_{e1} and r_{e2} are also defined for double curvature shapes in A2/5.2.3.

r_{e1} = R for circular shapes in Appendix 2, Figure 3, in mm (in.)

= $R_2 + (R_1 - R_2)\cos\theta$ for double curvature shapes in Appendix 2, Figure 4, in mm (in.)

= $(R_1 + R_2)/2$ for elliptical shapes in Appendix 2, Figure 5, in mm (in.)

r_{e2} = R for circular shapes in Appendix 2, Figure 3, in mm (in.)

= $R_1 - (R_1 - R_2)\sin\theta$ for double curvature shapes in Appendix 2, Figure 4, in mm (in.)

= R_2 for elliptical shapes in Appendix 2, Figure 5, in mm (in.)

k_{s1}

r_{s1}/w_1	0.1	0.2	0.3	0.4	0.5
k_{s1}	1.945	1.89	1.835	1.78	1.725

k_{s2}

r_{s2}/w_2	0.1	0.2	0.3	0.4	0.5
k_{s2}	2.35	2.20	2.05	1.90	1.75

Note: k_{s1} and k_{s2} may be obtained by interpolation for intermediate values of r_{s1}/w_1 or r_{s2}/w_2.

where

w_1 = width of the cross deck under consideration, in mm (in.), for hatch corners of the strength deck and lower deck

= $0.1b_1$ for width of cross deck that is not constant along hatch length

w_2 = width of the cross deck under consideration, in mm (in.), for strength deck and lower deck

b_1 = width of the hatch opening under consideration, in mm (in.)

K_{s1} and K_{s2} for hatch corners with configurations other than those specified in this Appendix are to be determined from fine mesh finite element analysis.

The angle, ϕ, in degrees, along the hatch corner contour is defined as shown in Appendix 2, Figures 3, 4, and 5, and c_{L1} and c_{L2} at a given ϕ may be obtained by the following equations. For determining the maximum f_R, c_{L1} and c_{L2} are to be calculated at least for 5 locations (i.e., at $\phi = \phi_1$, ϕ_2 and three intermediate angles for each pair of the combined load cases considered).

- For circular shapes, $25 \le \phi \le 55$

c_{L1} = $1 - 0.00045(\phi - 25)^2$

c_{L2} = $0.8 - 0.0004(\phi - 55)^2$

- For double curvature shapes, $\phi_1 \le \phi \le \phi_2$

c_{L1} = $[1.0 - 0.02(\phi - \phi_1)]/[1 - 0.015(\phi - \phi_1) + 0.00014(\phi - \phi_1)^2]$ for $\theta < 55$

= $[1.0 - 0.026(\phi - \phi_1)]/[1 - 0.03(\phi - \phi_1) + 0.0012(\phi - \phi_1)^2]$ for $\theta \ge 55$

c_{L2} = $0.8/[1.1 + 0.035(\phi - \phi_2) + 0.003(\phi - \phi_2)^2]$

where

ϕ_1 = $\mu(95 - 70r_{s1}/r_d)$

ϕ_2 = $95/(0.6 + r_{s1}/r_d)$

μ = $0.165(\theta - 25)^{1/2}$ for $\theta < 55$

= 1.0 for $\theta \ge 55$

- For elliptical shapes, $\phi_1 \le \phi \le \phi_2$

c_{L1} = $1 - 0.00004(\phi - \phi_1)^3$

c_{L2} = $0.8/[1 + 0.0036(\phi - \phi_2)^2]$

where

ϕ_1 = $95 - 70R_2/R_1$

ϕ_2 = $88/(0.6 + R_2/R_1)$

The peak stress range, f_R, is to be obtained through calculations of c_{L1} and c_{L2} at each ϕ along a hatch corner.

The formulas for double curvature shapes and elliptical shapes may be applicable to the following range:

$0.3 \le R_2/R_1 \le 0.6$ and $45° \le \theta \le 80°$ for double curvature shapes

For hatch coaming top and longitudinal deck girders, R_2/R_1 may be reduced to 0.15.

$0.3 \le R_2/R_1 \le 0.9$ for elliptical shapes

5.2.2 Hatch Corners at the End Connections of Longitudinal Deck Girder

The total stress range, f_R, for hatch corners at the connection of longitudinal deck girder with cross deck box beam may be approximated by the following equation:

$$f_R = 0.5^{1/\gamma} \times c_f(\alpha_i K_{d1} f_{RG1} + K_{d2} f_{RG2}) \quad \text{N/cm}^2\ (\text{kgf/cm}^2,\ \text{lbf/in}^2)$$

where

f_{RG1} = wave-induced stress range by hull girder vertical and horizontal bending moments and torsional moment at the longitudinal deck girder of hull girder section, in N/cm² (kgf/cm², lbf/in²)

= $|f_{d1vi} - f_{d1vj}| + |f_{d1hi} - f_{d1hj}| + |f_{d1wi} - f_{d1wj}|$

f_{RG2} = wave-induced stress range by hull girder torsional moment at the connection of the longitudinal deck girder with the cross deck box beam, in N/cm² (kgf/cm², lbf/in²)

= $|f_{d1di} - f_{d1dj}|$

α_i = 1.0 for symmetrical section of the longitudinal deck girder about its vertical neutral axis

= 1.25 for unsymmetrical section of the longitudinal deck girder about its vertical neutral axis

c_f and γ are as defined in A2/5.2.1 and A2/3.3.

$f_{d1vi}, f_{d1vj}, f_{d1hi}, f_{d1hj}, f_{d1wi}$, and f_{d1wj} are as defined in A2/5.2.1.

K_{d1} and K_{d2} may be obtained from the following equations, but not to be taken less than 1.0:

$K_{d1} = 1.0$

$K_{d2} = \alpha_t \alpha_s k_d$

where

k_d = nominal stress concentration factor as given in the table below

α_s = 1.0 for circular shapes

= $0.33[1 + 2(r_{s1}/r_d) + 0.1(r_d/r_{s1})^2]$ for double curvature shapes

= $0.33[1 + 2(R_2/R_1) + 0.1(R_1/R_2)^2]$ for elliptical shapes

α_t = $(t_d/t_i)^{1/2}$

α_t is to be taken as 1.0 where longitudinal or transverse extent of the reinforced plate thickness in way of the hatch corner is less than that in A2/5.2.3, as shown in Appendix 2, Figure 6.

t_d = flange plate thickness of the longitudinal deck girder clear of the hatch corner under consideration, in mm (in.)

t_i = plate thickness at the end connection of the longitudinal deck girder under consideration, in mm (in.).

R, R_1 and R_2 for each shape are as shown in Appendix 2, Figures 3, 4 and 5.

θ for double curvature shapes is defined in Appendix 2, Figure 4.

r_{s1} and r_d are as defined for double curvature shapes in A2/5.2.1, above.

r_{e1} and r_{e2} are as defined for double curvature shapes in A21/5.2.3, below.

k_d

r_{s1}/w_d	0.1	0.2	0.3	0.4	0.5
k_d	2.35	2.20	2.05	1.90	1.75

Note: k_d may be obtained by interpolation for intermediate values of r_{s1}/w_d.

where

w_d = width of the longitudinal deck girder, in mm (in.)

5.2.3 Extent of Reinforced Plate Thickness at Hatch Corners

Where plating of increased thickness is inserted at hatch corners, the extent of the inserted plate, as shown in Appendix 2, Figures 2 and 6, is to be generally not less than that obtained from the following:

$\ell_i = 1.75 r_{e1}$ mm (in.)

$b_i = 1.75 r_{e2}$ mm (in.)

$b_d = 1.1 r_{e2}$ mm (in.)

For a cut-out radius type:

$\ell_{i1} = 1.75 r_{e1}$ mm (in.)

$\ell_{i2} = 1.0 r_{e1}$ mm (in.)

$b_i = 2.5 r_{e2}$ mm (in.)

$b_d = 1.25 r_{e2}$ mm (in.)

where

r_{e1}	=	R	for circular shapes in Appendix 2, Figure 3, in mm (in.)
	=	$R_2 + (R_1 - R_2)\cos\theta$	for double curvature shapes in Appendix 2, Figure 4, in mm (in.)
	=	$(R_1 + R_2)/2$	for elliptical shapes in Appendix 2, Figure 5, in mm (in.)
r_{e2}	=	R	for circular shapes in Appendix 2, Figure 3, in mm (in.)
	=	$R_1 - (R_1 - R_2)\sin\theta$	for double curvature shapes in Appendix 2, Figure 4, in mm (in.)
	=	R_2	for elliptical shapes in Appendix 2, Figure 5, in mm (in.)

At welding joints of the inserted plates to the adjacent plates, a suitable transition taper is to be provided and the fatigue assessment at these joints may be approximated by the following:

$f_R = 0.5^{1/\gamma} \times c_f K_t f_s$ N/cm^2 (kgf/cm^2, lbf/in^2)

where

f_s	=	nominal stress range at the joint under consideration	
	=	f_{RG1}	for side longitudinal deck box, as specified in A2/5.2.1, in N/cm^2 (kgf/cm^2, lbf/in^2)
	=	f_{RG2}	for cross deck box beam, as specified in A2/5.2.1, in N/cm^2 (kgf/cm^2, lbf/in^2)
	=	$f_{RG1} + f_{RG2}$	for longitudinal deck girder, as specified in A2/5.2.2, in N/cm^2 (kgf/cm^2, lbf/in^2)
K_t	=	$0.25(1 + 3t_i/t_a) \le 1.25$	
t_i	=	plate thickness of inserted plate, in mm (in.)	
t_a	=	plate thickness of plate adjacent to the inserted plate, in mm (in.)	

c_f and γ are as defined in A2/5.2.1 and A2/3.3.

FIGURE 2
Hatch Corners at Decks and Coaming Top

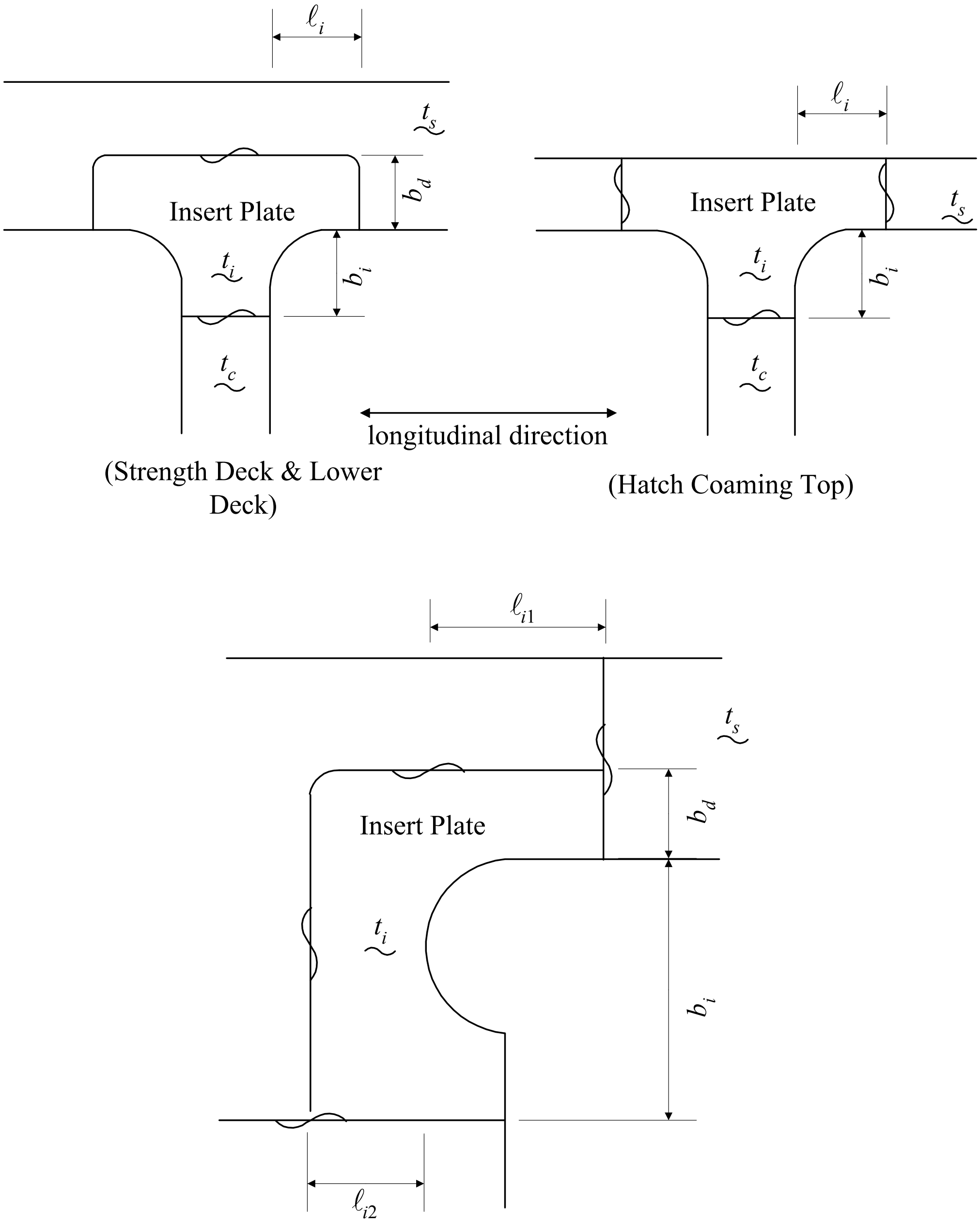


FIGURE 3
Circular Shape

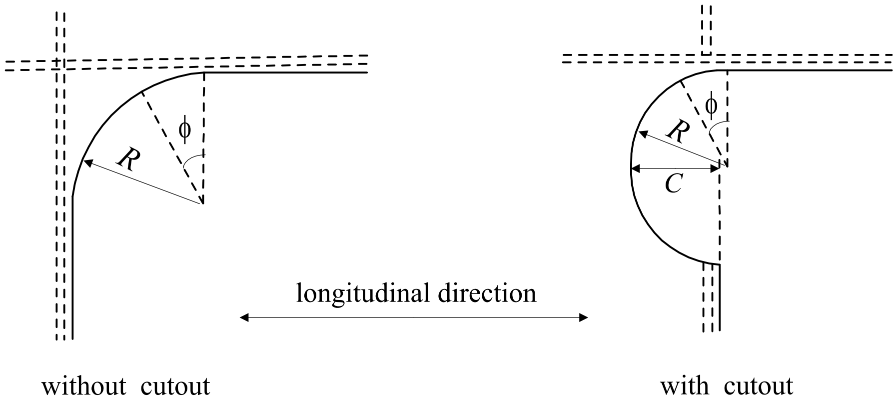


FIGURE 4
Double Curvature Shape

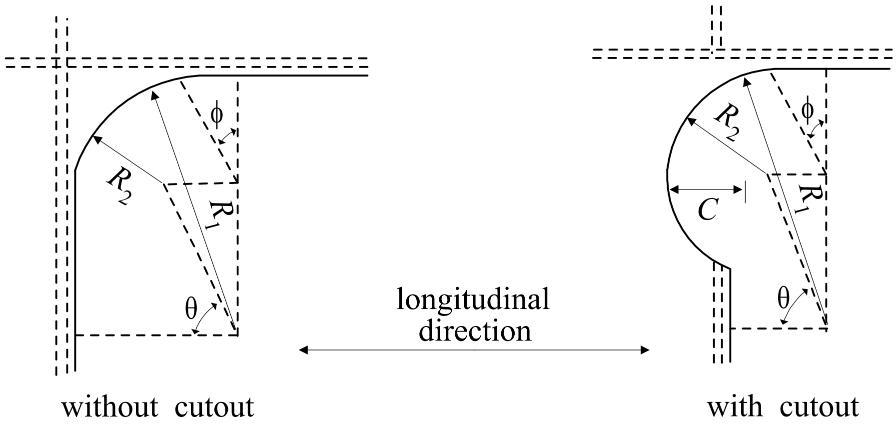


FIGURE 5
Elliptical Shape

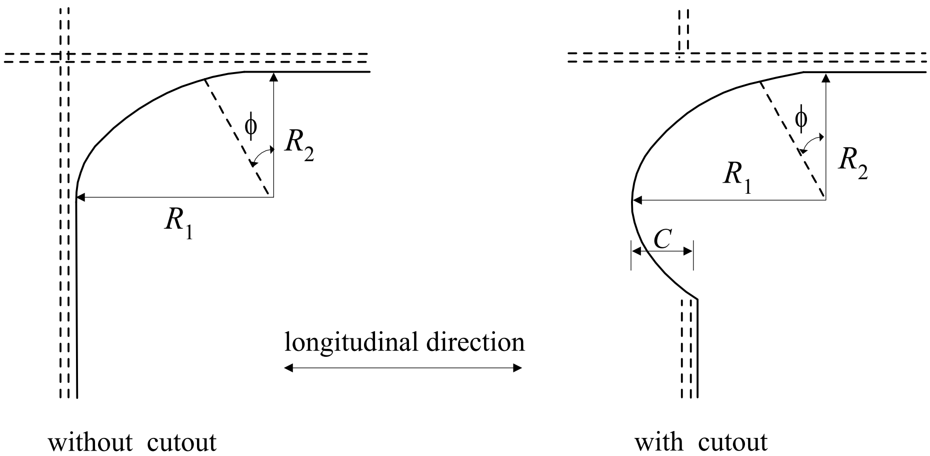
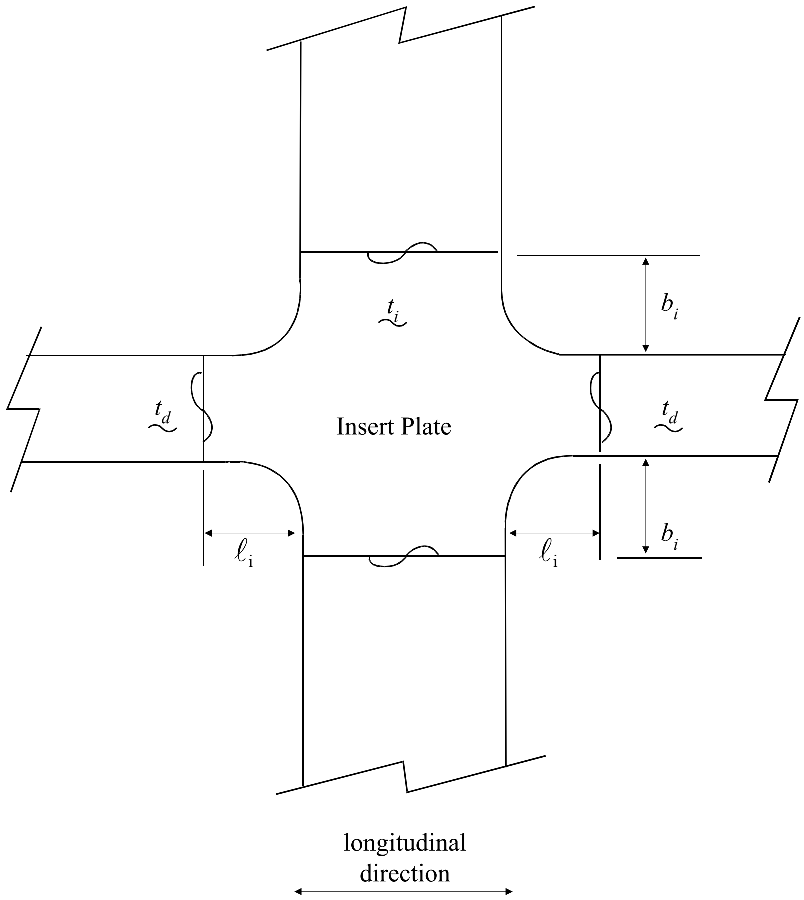


FIGURE 6
Hatch Corner for Longitudinal Deck Girder



6 Hot Spot Stress Approach with Finite Element Analysis

6.1 Introduction

In principle, the fatigue strength of all connections can be assessed with the hot spot stress approach described in this Subsection. However, for some details as indicated in A2/2.2, in lieu of the hot spot stress approach, the nominal stress approach can also be employed to evaluate the fatigue strength.

Hot spot stress is defined as the surface stress at the hot spot. Note that the stress change caused by the weld profile is not included in the hot spot stress, but the overall effect of the connection geometry on the nominal stress is represented. Therefore, in hot spot stress approach, the selection of an S-N curve depends on: 1) weld profile, i.e., existence of weld and weld type (fillet, partial penetration or full penetration); 2) predominant direction of principal stress; and 3) crack locations (toe, root or weld throat).

There are various adjustments (reductions in capacity) that may be required to account for factors such as a lack of corrosion protection (coating) of structural steel and relatively large plate thickness. The imposition of these adjustments on fatigue capacity will be in accordance with ABS practice for vessels.

There are other adjustments that could be considered to increase fatigue capacity above that portrayed by the cited S-N data. These include adjustments for compressive "mean stress" effects, a high compressive portion of the acting variable stress range, and the use of "weld improvement" techniques. The use of a weld improvement technique, such as weld toe grinding or peening to relieve ambient residual stress, can be effective in increasing fatigue life. However, credit should not be taken of such a weld improvement in the design phase of the structure. Consideration for granting credit for the use of weld improvement techniques is to be reserved for situations arising during construction, operation, or future reconditioning of the structure.

An exception may be made if the target design fatigue life cannot be satisfied by other preferred design measures such as refining layout, geometry, scantlings, and welding profile to minimize fatigue damage due to high stress concentrations. Grinding or ultrasonic peening can be used to improve fatigue life in such cases. The calculated fatigue life is to be greater than 15 years excluding the effects of life improvement techniques. Where improvement techniques are applied, full details of the improvement technique standard including the extent, profile smoothness particulars, final weld profile, and improvement technique workmanship and quality acceptance criteria are to be clearly shown on the applicable drawings and submitted for review together with supporting calculations indicating the proposed factor on the calculated fatigue life.

Grinding is preferably to be carried out by rotary burr and to extend below the plate surface in order to remove toe defects, and the ground area is to have effective corrosion protection. The treatment is to produce a smooth concave profile at the weld toe with the depth of the depression penetrating into the plate surface to at least 0.5 mm below the bottom of any visible undercut. The depth of groove produced is to be kept to a minimum, and, in general, kept to a maximum of 1 mm. In no circumstances is the grinding depth to exceed 2 mm or 7% of the plate gross thickness, whichever is smaller. Grinding is to extend to areas well outside the highest stress region.

The finished shape of a weld surface treated by ultrasonic peening is to be smooth, and all traces of the weld toe are to be removed. Peening depths below the original surface are to be maintained to at least 0.2 mm. Maximum depth is generally not to exceed 0.5 mm.

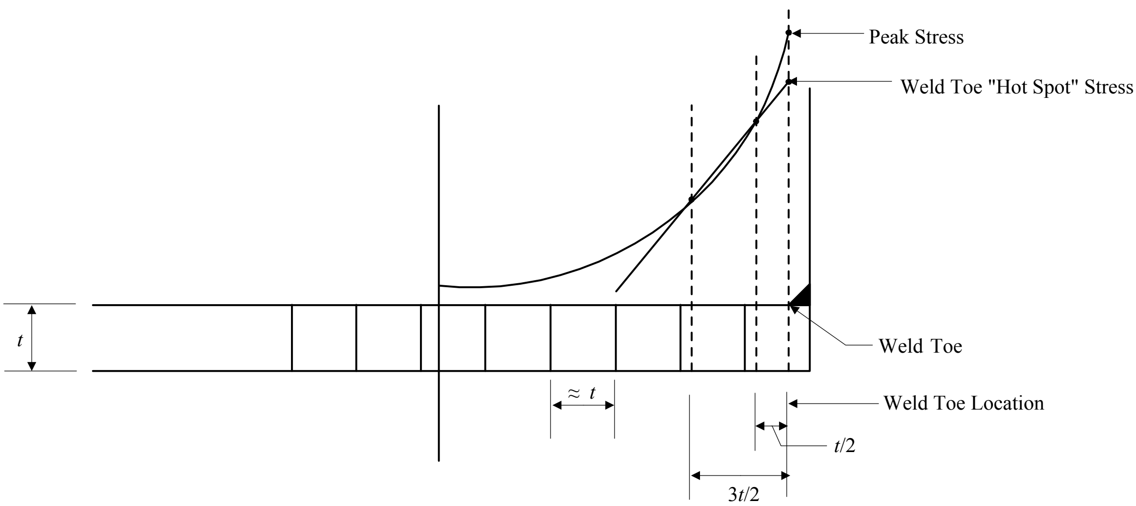
Provided these recommendations are followed, an improvement in fatigue life by grinding or ultrasonic peening up to a maximum of 2 times may be granted.

6.2 Calculation of Hot Spot Stress at a Weld Toe

Appendix 2, Figure 7 shows an acceptable method which can be used to extract and interpret the "near weld toe" element dynamic stress ranges (referred to as stresses for convenience in the following text in this Subsection) and to obtain a (linearly) extrapolated stress (dynamic stress range) at the weld toe. When plate or shell elements are used in the modeling, it is recommended that each element size is to be equal to the plate thickness.

Weld hot spot stress can be determined from linear extrapolation of surface component stresses at $t/2$ and $3t/2$ from weld toe. The principal stresses at hot spot are then calculated based on the extrapolated stresses and used for fatigue evaluation. Description of the numerical procedure is given below.

FIGURE 7



The algorithm described in the following is applicable to obtain the hot spot stress for the point at the toe of a weld. The weld typically connects either a flat bar member or a bracket to the flange of a longitudinal stiffener, as shown in Appendix 2, Figure 8.

Consider the four points, P_1 to P_4, measured by the distances X_1 to X_4 from the weld toe, designated as the origin of the coordinate system. These points are the centroids of four neighboring finite elements, the first of which is adjacent to the weld toe. Assuming that the applicable surface component stresses (or dynamic stress ranges), S_i, at P_i have been determined from FEM analysis, the corresponding stresses at "hot spot" (i.e., the stress at the weld toe) can be determined by the following procedure:

6.2.1

Select two points, L and R, such that points L and R are situated at distances $t/2$ and $3t/2$ from the weld toe; i.e.:

$$X_L = t/2, \qquad X_R = 3t/2$$

where t denotes the thickness of the member to which elements 1 to 4 belong (e.g., the flange of a longitudinal stiffener).

6.2.2

Let $X = X_L$ and compute the values of four coefficients, as follows:

$$C_1 = [(X - X_2)(X - X_3)(X - X_4)] / [(X_1 - X_2)(X_1 - X_3)(X_1 - X_4)]$$

$$C_2 = [(X - X_1)(X - X_3)(X - X_4)] / [(X_2 - X_1)(X_2 - X_3)(X_2 - X_4)]$$

$$C_3 = [(X - X_1)(X - X_2)(X - X_4)] / [(X_3 - X_1)(X_3 - X_2)(X_3 - X_4)]$$

$$C_4 = [(X - X_1)(X - X_2)(X - X_3)] / [(X_4 - X_1)(X_4 - X_2)(X_4 - X_3)]$$

The corresponding stress at Point L can be obtained by interpolation as:

$$S_L = C_1S_1 + C_2 S_2 + C_3 S_3 + C_4 S_4$$

6.2.3

Let $X = X_R$ and repeat the step in A2/6.2.2 to determine four new coefficients. The stress at Point R can be interpolated likewise, i.e.:

$$S_R = C_1S_1 + C_2 S_2 + C_3 S_3 + C_4 S_4$$

6.2.4

The corresponding stress at hot spot, S_0, is given by

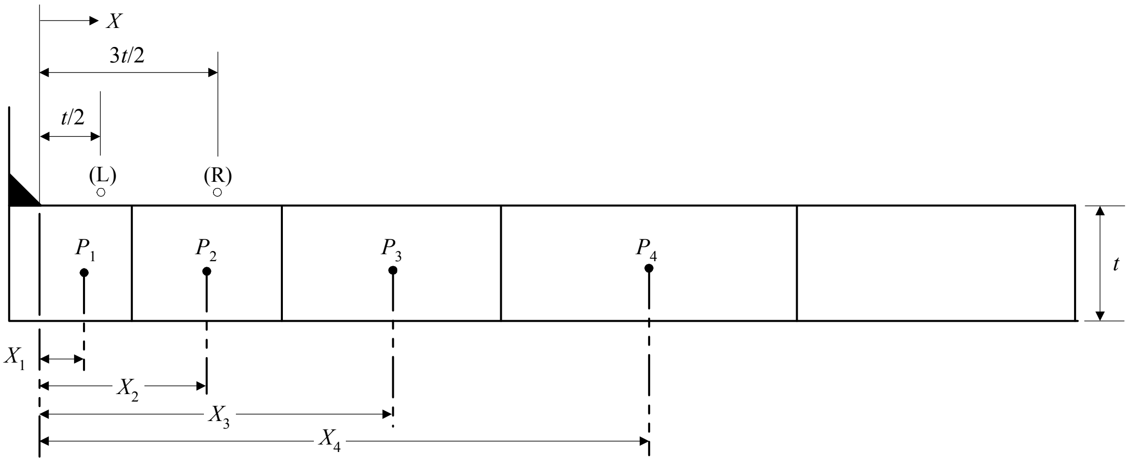
$$S_0 = (3S_L - S_R)/2$$

Notes:

The algorithm presented in the foregoing involves two types of operations. The first is to utilize the stress values at the centroid of the four elements considered to obtain estimates of stress at Points L and R by way of an interpolation algorithm known as Lagrange interpolation. The second operation is to make use of the stress estimates, S_L and S_R, to obtain the hot spot stress via linear extrapolation.

While the Lagrange interpolation is applicable to any order of polynomial, it is not advisable to go beyond the 3rd order (cubic). Also, the even order polynomials are biased, so that leaves the choice between a linear scheme and a cubic scheme. Therefore, the cubic interpolation, as described in A2/6.2.2, should be used. It can be observed that the coefficients, C_1 to C_4 are all cubic polynomials. It is also evident that, when $X = X_j$, which is not equal to X_i, all of the C's vanish except C_i, and if $X = X_i$, $C_i = 1$.

FIGURE 8



6.3 Calculation of Hot Spot Stress at the Edge of Cut-out or Bracket

In order to determine the hot spot stress at the edge of cut-out or bracket, dummy rod elements can be attached to the edge. The sectional area of the dummy rod may be set at 0.01 cm^2. The mesh needs to be fine enough to determine the local stress concentration due to the geometry change. The axial stress range of the dummy rod is to be used to assess the fatigue strength of the cut-out or bracket (edge crack).