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RECOMMENDED PRACTICE  
DNV-RP-F204

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RISER FATIGUE

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OCTOBER 2010

DET NORSKE VERITAS

# FOREWORD

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**Acknowledgement:**

The following NDP member companies are gratefully acknowledged for their contributions to this Recommended Practice:

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ConocoPhillips Norge

Chevron Texaco

Exxon Mobil

Marintek

Norsk Hydro

NPD

RWE Dea Norge

Shell

Statoil

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DNV is grateful for the valuable co-operations and discussions with the individual personnel of these companies.

**CHANGES**

• **General**

As of October 2010 all DNV service documents are primarily published electronically.

In order to ensure a practical transition from the “print” scheme to the “electronic” scheme, all documents having incorporated amendments and corrections more recent than the date of the latest printed issue, have been given the date October 2010.

An overview of DNV service documents, their update status and historical “amendments and corrections” may be found through [http://www.dnv.com/resources/rules\\_standards/](http://www.dnv.com/resources/rules_standards/).

• **Main changes**

Since the previous edition (July 2005), this document has been amended, most recently in April 2009. All changes have been incorporated and a new date (October 2010) has been given as explained under “General”.



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# 1. Introduction

## 1.1 General

Fatigue is addressed by all recognized standards and codes, which call for adequate safety against fatigue failure. This Recommended Practice presents recommendations in relation to riser fatigue analyses based on fatigue tests and fracture mechanics. Conditions for the validity of the Recommended Practice are given in Section 1.3.

The aim of fatigue design is to ensure that the risers have adequate fatigue life. Calculated fatigue lives also form the basis for efficient inspection programmes during fabrication and the operational life of the risers.

To ensure that the risers will fulfil its intended function, a fatigue assessment, supported where appropriate by a detailed fatigue analysis, should be carried out for each representative riser, which is subjected to fatigue loading. It should be noted that any element or part of the riser, every welded joint and attachments or other form of stress concentration, is potentially a source of fatigue cracking and should be individually considered.

## 1.2 Objective

The objective of this document is to outline the methodology for performing fatigue assessment of metallic risers subjected to repeated load fluctuations and supplement the details on fatigue analysis methods recommended in DNV-OS-F201. See also DNV-RP-C203.

## 1.3 Application

The assessment procedure assumes that the riser has been designed in accordance with any recognized code, such as DNV-OS-F201.

This recommended practice can be applied to all types of metallic risers, where the fatigue limit state needs to be considered. However, the standard design fatigue factors given in Section 6.2 are only applicable to steel risers.

## 1.4 Safety philosophy and safety class

The safety philosophy and design principles adopted in DNV-OS-F201 apply. The basic principles are in agreement with most recognised codes and reflect state-of-the-art industry practice and latest research.

Riser design should be based on potential failure consequences. This is implicit by the concept of safety classes defined in Table 1-1. See DNV-OS-F201 Sec.2 C200, for a more detailed description of the safety class methodology.

Safety class	Definition
Low	Where failure implies low risk of human injury and minor environmental and economic consequences.
Normal	For conditions where failure implies risk of human injury, significant environmental pollution or very high economic or political consequences.
High	For operating conditions where failure implies high risk of human injury, significant environmental pollution or very high economic or political consequences.

The target safety level shall be based on the safety class as given in Table 1-2. The values are nominal values reflecting structural failure due to normal variability in load and resistance but excluding gross error.

Safety class		
Low	Normal	High
$10^{-3}$	$10^{-4}$	$10^{-5}$

The following comments apply for the Table 1-2:

- 1) The failure probability from a structural reliability analysis is a nominal value and cannot be interpreted as an expected frequency of failure.
- 2) The probability basis (i.e. annual per riser) is failures per year for permanent conditions and for the actual period of operation for temporary conditions.

The FLS probability basis is failures per year, i.e., often last year of service life or last year before inspection.

### Guidance note:

If the service life of a riser is 20 years, the FLS probability basis is failures per year, while moving from 19<sup>th</sup> year to the 20<sup>th</sup> year. Alternatively, it can be defined, with respect to periodicity of inspection, i.e. if the riser is inspected every 5<sup>th</sup> year, the FLS probability basis is failures per year, while moving from 4<sup>th</sup> year to the 5<sup>th</sup> year.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

## 1.5 Relationship to other design codes

This Recommended Practice formally supports and complies with DNV-OS-F201, and is recognised as a supplement to relevant National rules and regulations.

Further, this Recommended Practice is supported by other DNV Offshore Codes as follows:

- DNV-OS-F101, “Submarine Pipeline Systems”
- DNV-RP-C203 “Fatigue Strength Analysis of Offshore Steel Structures”
- DNV-RP-C205 “Environmental Conditions and Environmental Loads”
- DNV-RP-F105 “Free Spanning Pipelines”

and of the API Recommended Practice:

- API RP 2 RD “Design of Risers for Floating Production Systems (FPSs) and Tension-Leg Platforms (TLPs)”.

## 1.6 Definitions

**Corrosion allowance:** The amount of wall thickness added to the pipe or component to allow for corrosion/erosion/wear.

**Design Fatigue Factors (DFF):** Safety factor applied to increase the probability for avoiding fatigue failures. DFFs shall be applied to the service life and the calculated fatigue life shall be longer than the product of the service life and DFF.

**Environmental loads:** Loads due to the environment, such as waves, current, wind, etc.

**Extended Service Life:** An extension to the original intended service life of a component, which exceeds the *Service Life* planned at the design stage. Extended service life is duration calculated from the time of installation till the completion of the extended service lifetime.

**Failure:** An event causing an undesirable condition, e.g. loss of component or system function, or deterioration of functional capability to such an extent that the safety of the unit, personnel or environment is significantly reduced.

**Fatigue:** Cyclic loading causing degradation of the material.

**Fatigue Limit State (FLS):** Related to the possibility of failure due to the effect of cyclic loading.

**Floater:** Buoyant installation, which is floating or fixed to the sea bottom by mooring systems in temporary or permanent phases, e.g. TLP, Ship, Semi, Spar, Deep Draft Floater etc.

**Floater offset:** The total offset of the floater, taking into account the floater mean offset, wave frequency motions and low frequency wind and wave motions.

**Floater mean offset:** The offset created by steady forces from

current, wind and waves.

**Floater wave frequency motions:** The motions that are a direct consequence of first order wave forces acting on the floater, causing the platform to move at periods typically between 3 – 25 seconds, and termed the wave frequency (WF) regime.

**Fracture Analysis:** Analysis where critical initial defect sizes under design loads are identified to determine the crack growth life to failure, i.e. leak or unstable fracture.

**Global Analysis:** Analysis of the complete riser system.

**Inspection:** Activities such as measuring, examination, testing, gauging one or more characteristics of an object or service and comparing the results with specified requirements to determine conformity.

**Installation:** The operation related to installing the riser system, such as running of riser joints, landing and connecting or such as laying, tie-in, etc. for a catenary riser

**Limit State:** The state beyond which the riser or part of the riser no longer satisfies the requirements laid down to its performance or operation. Examples are structural failure (rupture, local buckling) or operational limitations (stroke or clearance).

**Load:** The term load refers to physical influences which cause stress, strain, deformation, displacement, motion, etc. in the riser.

**Load Effect:** Response or effect of a single load or combination of loads on the structure, such as bending moment, effective tension, stress, strain, deformation, etc.

**Load and Resistance Factor Design (LRFD):** Design format based upon a limit state and partial safety factor methodology. The partial safety factor methodology is an approach where separate factors are applied for each load effect (response) and resistance term.

**Location class:** A geographic area classified according to the distance from locations with regular human activities.

**Low Frequency (LF) motion:** Motion response at frequencies below wave frequencies at, or near surge, sway and yaw eigenperiods for the floater. LF motions typically have periods ranging from 30 to 300 seconds.

**Non-destructive testing (NDT):** Structural tests and inspection of welds or parent material with radiography, ultrasonic, magnetic particle or eddy current testing.

**Offshore Standard (OS):** Offshore Standard: The DNV offshore standards are documents which present the principles and technical requirements for design of offshore structures. The standards are offered as DNV's interpretation of engineering practice for general use by the offshore industry for achieving safe structures.

**Operation, Normal Operation:** Conditions that are part of routine (normal) operation of the riser system.

**Prior Service Life:** The duration that a component has been in service, since its installation. Duration is computed from the time of installation or production if relevant.

**Recommended Practice (RP):** The recommended practice publications cover proven technology and solutions which have been found by DNV to represent good practice, and which represents one of the possible alternatives for satisfying the requirements stipulated in the DNV offshore standards or other codes and standards cited by DNV.

**Reduced Velocity:** Non-dimensional velocity parameter used

for assessing vortex induced vibration (VIV) due to the vortex shedding force.

**Residual Service Life:** The duration that a component will be in service, from this point forward in time (from now). Duration is computed from now until the component is taken out of service.

**Riser Array:** Riser system consisting of vertical or near vertical risers. Typically up to 20 risers distributed in a cluster.

**Riser component:** Any part of the riser system that may be subjected to pressure by the internal fluid. This includes items such as flanges, connectors, stress joints, tension joints, flexible joints, ball joints, telescopic joints, slick joints, tees, bends, reducers and valves.

**Riser joint:** A riser joint consists of a pipe member mid section, with riser connectors at each end. Riser joints are typically provided in 30 ft. to 50 ft. (9,14m to 15,24m) lengths. Shorter joints, "pup joints", may also be provided to ensure proper space-out.

**Riser pipe (riser tube):** The pipe, which forms the principal conduit of the riser joint. For example, the riser pipe is the conduit for containing the production fluid flow from the well into the surface tree.

**Riser Tensioner System:** A device that applies a tension to the riser string while compensating for the relative vertical motion (stroke) between the floater and riser. Tension variations are controlled by the stiffness of the unit.

**Risk Based Safety Factors:** Risk Based Case Specific Safety factor applied to increase the probability for avoiding fatigue failures.

**Safety Class:** The concept adopted herein to classify the criticality of the riser system.

**Safety Class Factors:** Factors applied in the 'risk based fatigue criterion', where the factors are linked to the safety class.

**Safety Factors:** Same as Design Fatigue Factors.

**Screening Analysis:** Used to frame the problem in order to identify if analyses and methods that are more advanced should be employed.

**Service Life:** The length of time assumed in design that a component will be in service.

**S-N Fatigue Curve:** Stress range versus number of cycles to failure.

**Stress amplification factor (SAF):** Equal to the local peak alternating principal stress in a component (mechanical connectors) divided by the nominal alternating principal stress near the location of the component. This factor is used to account for the increase in the stresses caused by geometric stress amplifiers which occur in the riser component.

**Stress Concentration Factor (SCF):** Equal to the local peak alternating principal stress in a component (including welds) divided by the nominal alternating principal stress near the location of the component. This factor is used to account for the increase in the stresses caused by geometric stress amplifiers, which occur in the riser component.

**Uncertainty:** In general the uncertainty can be described by a probability distribution function. In the context of this Recommended Practice, the probability distribution function is described in terms of bias and standard deviation of the variable.

## 1.7 Abbreviations

API	American Petroleum Institute
CF	Cross-flow
DFF	Design Fatigue Factor
DFI	Design Fabrication Installation
DNV	Det Norske Veritas
EPC	Engineering Procurement Construction
FD	Frequency-Domain
FEED	Front End Engineering Design
FEM	Finite Element Method
FLS	Fatigue Limit State
FPSO	Floating Production Storage Offloading
IL	In-line
LF	Low Frequency
LRFD	Load and Resistance Factor Design
LTD	Linearized Time-Domain
MWL	Mean Water Level
NB	Narrow Banded
NDP	Norwegian Deepwater Program
NLTD	Non-Linear Time-Domain
RFC	Rain Flow Counting
RP	Recommended Practice
SCF	Stress Concentration Factor
SCR	Steel Catenary Riser
TD	Time-Domain
TDP	Touch Down Point
TDZ	Touch Down Zone
TLP	Tension Leg Platform
TTR	Top Tensioned Riser
VIV	Vortex Induced Vibrations
WF	Wave Frequency
2D	Two-dimensional
3D	Three-dimensional

## 1.8 Symbols

$\bar{a}$	Intercept of the design S-N curve with the log N axis
$a_{i,rms}^{CF}$	Cross flow VIV rms amplitude for mode number $i$
$a_{i,rms}^{IL}$	In-line VIV rms amplitude for mode number $i$
$A_{rms}^{CF}$	Rms value of displacement amplitude for cross flow VIV response
$A_{rms}^{IL}$	Rms value of displacement amplitude for in-line VIV response
$D$	Fatigue damage; Outer diameter
$D_{fat}$	Accumulated fatigue damage
$D_{VIV-ST}$	Fatigue damage from the extreme VIV event (Short term event)
$D_{VIV}$	Accumulated fatigue damage from VIV only
DFF	Design Fatigue Factor
$DFF_{VIV}$	Design Fatigue Factor for VIV
$DFF_{VIV-ST}$	Design fatigue factor for the extreme VIV event (Short term event).
$D_h$	Hydrodynamic diameter
$D_i$	Short term fatigue damage / Internal diameter

$D_{Prior}$	Computed fatigue damage per year during the Prior service lifetime
$D_{Residual}$	Computed fatigue damage per year for the Residual service lifetime
$D_s(x)$	Stochastic fatigue damage
$D_s$	Strength diameter (Steel outer diameter)
$D(\mu_x)$	Base case fatigue damage (deterministic)
$E$	Elastic modulus
$f^{CF}$	Cross flow VIV oscillation frequency
$f^{IL}$	In-line VIV oscillation frequency
$f_0$	Mean number of stress cycles per unit time
$f_s$	Strouhal frequency
$f_S(s)$	Probability density function (PDF) for the stress cycles
$f_v$	Mean frequency of stress cycles
$I$	Moment of inertia of tubulars
$I_{factor}$	Relative importance factor
$k$	Thickness exponent
$L_{exc}$	Excitation length for VIV calculation
$m, m_i$	Negative inverse slope of the S-N curve; crack growth parameter
$N$	Number of cycles to failure at constant stress range $S$
$N_{sw}$	Number of cycles at which change in slope appears for the 2 slope S-N curve
$N_s$	Number of discrete sea states in the wave scatter diagram
$P_f$	Failure probabilities (annual per riser)
$P_i$	Sea state probability
$S_0$	Nominal stress range
SCF	Stress concentration factor
$S_{t,eff}$	Effective Strouhal number related to $U_{eff}$
$S_{sw}$	Stress at intersection of the 2 slope S-N curve
$t_{corr}$	Corrosion allowance
$t_{fat}$	Fatigue thickness
$t_{nom}$	Nominal (specified) riser pipe wall thickness
$t_{ref}$	Reference thickness
$T$	Design life time in years
$T_e$	Effective tension
$T_{Calculated}$	Fatigue life time calculated in years, without the DFF
$T_{Extended}$	Extended service life time in years
$T_{Prior}$	Prior service life time in years
$T_{Residual}$	Residual service life time in years
$U_{eff}$	Effective flow velocity for VIV calculation
$X_D$	Normalized fatigue utilization
$X_i$	Stochastic variable
$X_{mod}$	Model uncertainty
$y, z$	Local co-ordinate system

## 1.9 Greek characters

$\alpha$	Bias
$\Delta$	Frequency bandwidth parameter for VIV
$\gamma$	Safety factor
$\gamma_{SC}$	Safety class factor accounting for the failure consequence
$\kappa$	Curvature for mode number $i$
$\theta$	Angular coordinate
$\rho$	Fluid (water) density
$\sigma$	Standard deviation ; Nominal stress
$s_a$	axial stress
$s_M$	bending stress
$\sigma_{x_a}$	Standard deviation of the log of the intercept of the S-N curve
$\sigma_s$	Stress standard deviation
$\sigma_{x_D}$	Uncertainty in fatigue damage (Standard deviation of Normalized fatigue utilization)

## 1.10 Organisation of this Recommended Practice

This Recommended Practice is organised as follows:

**Section 1** contains the objectives and scope. It further introduces essential concepts, definitions and abbreviations.

**Section 2** contains the fundamental fatigue design philosophy and fatigue assessment procedures. It introduces the basic fatigue damage methodology and global fatigue analysis procedures for the wave frequency and the low frequency fatigue.

**Section 3** describes the guiding principles for selecting the appropriate S-N curves and also provides guidance for qualification of S-N curves.

**Section 4** provides an overview of Vortex-Induced riser response, ranging from simplified screening criteria to comprehensive analysis techniques.

**Section 5** discusses requirements for combing wave frequency, low frequency and VIV fatigue damage. A combined acceptance criterion is also presented.

**Section 6** describes the Design Fatigue Factors that are to be applied in conjunction with the acceptance criteria. For traditional riser concepts known to possess acceptable reliability, the standard safety factors are presented. For all other design cases, the methodology for computing the case specific risk based safety factors is also presented.

**Section 7** describes the in-service inspection methodology and the methodology for performing a reassessment of the residual fatigue life. It also focuses on issues that need consideration when service life is planned to be extended.

**Section 8** contains the basic references used in this Recommended Practice.

## 2. Fatigue design

### 2.1 General

In general, the fatigue life of a component can be broken down into two phases: Crack initiation and Crack propagation. In the case of un-welded components (e.g., seamless pipes and machined components), the crack initiation period represents the bulk of the total fatigue life. This is particularly noticeable at high fatigue lives where the fatigue crack initiation period may exceed 95% of the fatigue life. In the case of machined components, once a fatigue crack has grown to a detectable size, the component is virtually at the end of its useful life and will normally be withdrawn from service if repair is not possible.

In the case of welded joints, weld toe/root discontinuities are generally present. These behave as pre-existing cracks. Consequently, the bulk of the fatigue life of a welded joint can be attributed to fatigue crack propagation.

The difference in the crack initiation phase of parent material and welded joints has significant effects on overall fatigue performance. In general, the fatigue strength of an un-welded component increases with material tensile strength due to the increased initiation life associated with higher strength materials. In the case of welded joints however, the fatigue strength is relatively unaffected by material tensile strength because the bulk of the fatigue life of a welded joint is spent in the propagation phase. Although crack propagation rates can change from one material to another and from one environment to another, there is no consistent trend with regard to tensile strength.

### 2.2 Fatigue assessment using S-N curves

This Recommended Practice primarily focuses on the fatigue assessments based on the S-N curve approach. When appropriate, the fatigue analysis may alternatively be based on fracture mechanics, as briefly described in Section 2.6.

A typical sequence in fatigue design of a riser is shown in Table 2-1.

The fatigue criterion, which shall be satisfied, may be written:

$$D_{fat} \cdot DFF \leq 1. \quad (2.1)$$

where

$D_{fat}$  = Accumulated fatigue damage (Palmgren-Miner rule)

DFF = Design Fatigue Factor, see Section 6

Task	Comment
Define fatigue loading.	Based on operating limitations including WF, LF and possible VIV load effects.
Identify locations to be assessed.	Structural discontinuities, joints (girth pipe welds, connectors, bolts), anode attachment welds, repairs, etc.
Global riser fatigue analysis.	Calculate short-term nominal stress range distribution at each identified location.
Local joint stress analysis.	Determination of the hot-spot SCF from parametric equations or detailed finite element analysis.
Identify fatigue strength data.	S-N curve depends on environment, construction detail and fabrication among others.
Identify thickness correction factor.	Apply thickness correction factor to compute resulting fatigue stresses.
Fatigue analyses.	Calculate accumulated fatigue damage from weighted short-term fatigue damage.
Further actions if too short fatigue life.	Improve fatigue capacity using: <ul style="list-style-type: none"> <li>— more refined stress analysis</li> <li>— fracture mechanics analysis</li> <li>— change detail geometry</li> <li>— change system design</li> <li>— weld profiling or grinding</li> <li>— improved inspection /replacement programme</li> </ul>

The design S-N curve should be based on the mean-minus-two-standard deviations curves for the relevant experimental data, see DNV-RP-C203.

The basic fatigue capacity is given in terms of S-N curves expressing the number of stress cycles to failure, N, for a given

constant stress range, S:

$$N = \bar{a} S^{-m} \quad (2.2)$$

or equivalently:

$$\log(N) = \log(\bar{a}) - m \log(S) \quad (2.3)$$

where  $\bar{a}$  and  $m$  are empirical constants established by experiments.

The stress range to be applied in fatigue damage calculations is found by application of a stress concentration factor as well as a thickness correction factor to the nominal stress range:

$$S = S_0 \cdot SCF \cdot \left( \frac{t_{fat}}{t_{ref}} \right)^k \quad (2.4)$$

where:

$S_0$  = Nominal stress range  
SCF = Stress concentration factor

$\left( \frac{t_{fat}}{t_{ref}} \right)^k$  = Thickness correction factor

The average representative pipe wall thickness is denoted as  $t_{fat}$  and is defined in equation (2.11). The thickness correction factor applies for pipes with a wall thickness  $t_{fat}$  greater than a reference wall thickness,  $t_{ref} = 25\text{mm}$ . The thickness exponent,  $k$ , is a function of the actual structural design and hence also related to S-N curve, see DNV RP-C203 Sec 2, for further details.

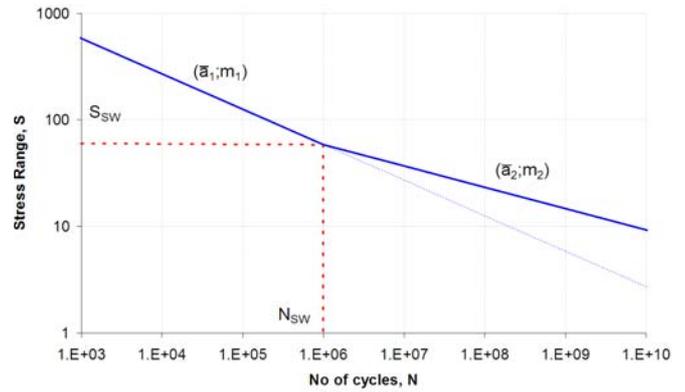
Bilinear (two-slope) S-N curves in log-log scale are frequently applied for representation of the experimental fatigue capacity data, i.e.

$$N = \begin{cases} \bar{a}_1 \cdot S^{-m_1} & S > S_{sw} \\ \bar{a}_2 \cdot S^{-m_2} & S \leq S_{sw} \end{cases} \quad (2.5)$$

$m_1$  and  $m_2$  are fatigue exponents (the inverse slope of the bi-linear S-N curve) and  $\bar{a}_1$  and  $\bar{a}_2$  are characteristic fatigue strength constant defined as the mean-minus-two-standard-deviation curve.  $S_{sw}$  is the stress at intersection of the two S-N curves given by:

$$S_{sw} = 10^{\left( \frac{\log(\bar{a}_1) - \log(N_{sw})}{m_1} \right)} \quad (2.6)$$

$N_{sw}$  is the number of cycles for which change in slope appears.  $\log(N_{sw})$  is typically 6-7. For further details see DNV RP-C203.



**Figure 2-1**  
Basic definitions for two-slope S-N curves

The Miner-Palmgren rule is adopted for accumulation of fatigue damage from stress cycles with variable range:

$$D = \sum_i \frac{n(S_i)}{N(S_i)} \quad (2.7)$$

Where  $n(S_i)$  is the number of stress cycles with range  $S_i$  and  $N(S_i)$  is the number of stress cycles to failure as expressed by (2.3).

The expected fatigue damage per unit time can for a linear S-N curve in log-log scale be expressed as:

$$D = \frac{f_0}{\bar{a}} \int_0^{\infty} s^m f_S(s) ds = \frac{f_0}{\bar{a}} E[S^m] \quad (2.8)$$

Where  $f_0$  is the mean number of stress cycles per unit time and  $f_S(s)$  is the probability density function (PDF) for the stress cycles. The expected fatigue damage is hence directly related to the  $m$ -th order moment,  $E[S^m]$  (or  $\mu_m$ ) of the stress cycle PDF. For a bi-linear S-N curve in log-log scale the corresponding expression becomes:

$$D = \frac{f_0}{\bar{a}_2} \int_0^{S_{sw}} s^{m_2} f_S(s) ds + \frac{f_0}{\bar{a}_1} \int_{S_{sw}}^{\infty} s^{m_1} f_S(s) ds \quad (2.9)$$

Equation (2.8) and (2.9) constitutes the basic formulation for assessment of the short-term fatigue damage in each stationary environmental condition as expressed by (2.10).

Equations (2.8) and (2.9) can also be applied to compute the long-term fatigue damage directly from the long-term distribution of stress cycles. For an introduction to methodology for establishment of long-term response distributions, see DNV-OS-F201.

It is straight forward to extend the above mentioned approach to 3-slope S-N curves.

### 2.3 Fatigue damage assessment procedure

Fatigue analysis of the riser system should consider all relevant cyclic load effects including:

- first order wave effects (direct wave loads and associated floater motions)
- second order floater motions
- vortex induced vibrations
- thermal and pressure induced stress cycles
- collisions
- floater hull VIV motions for spars and other deep draft floaters

- internal fluid slugging effects
- other concept specific loading conditions, such as springing motion of TLPs
- fabrication and installation loads.

All modes of operations including connected, running and hang-off must be considered if relevant. The relative importance of these contributions is case specific. Guidance is provided within this RP, for three important contributions to fatigue damage, arising from the wave-induced, the low-frequency and the vortex-induced stress cycles. The former two are addressed in this section, while the latter is described in Section 4.

A general approach for calculation of wave- and low-frequency fatigue damage contributions is based on application of the following procedure:

- the wave environment scatter diagram is subdivided into a number of representative blocks
- within each block, a single sea-state is selected to represent all the sea-states within the block. The probabilities of occurrence for all sea-states within the block are lumped to the selected sea-state.

**Guidance note:**

The selected sea-state from each block should give equal or greater damage than all the original sea-states within the block.

Wave directionality binning may also be adopted, in addition to the Hs-Tp binning.

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- the fatigue damage is computed for each selected short-term sea-state for all the blocks;
- the weighted fatigue damage accumulation from all sea-states can be expressed as:

$$D_{fat} = \sum_{i=1}^{N_s} D_i P_i \tag{2.10}$$

where

- $D_{fat}$  = Long-term fatigue damage
- $N_s$  = Number of discrete sea states in the wave scatter diagram
- $P_i$  = Sea state probability. Normally parameterised in terms of significant wave height, peak period and wave direction, i.e.  $P(H_s, T_p, \theta)_i$
- $D_i$  = Short term fatigue damage.

**2.4 Global fatigue analysis procedures**

The basis for fatigue damage calculations is global load effect analyses to establish the stress cycle distributions in a number of stationary short-term environmental conditions. The general principles for selection of analysis methodology and verification of simulation model are outlined in DNV-OS-F201.

The environmental loads are discussed in the context of riser analysis in DNV-OS-F201 Sec.3 D100-D500. The principles and methods as described in DNV-RP-C205 should be used as a basis when establishing the environmental load conditions.

The short-term fatigue conditions should be selected carefully to give an adequate representation of the stress cycles for the lifetime of the riser system. The selection must be based on a thorough physical knowledge regarding static- and dynamic behaviour of the riser system with special attention to FE modelling, hydrodynamic loading, resonance dynamics and floater motion characteristics. Sensitivity studies should be performed to support rational conservative assumptions regarding identi-

fied uncertain parameters (e.g. soil properties for fatigue analysis in the touch-down area of SCR's)

Fatigue analysis will normally involve global load effect analyses in a number of low- to moderate sea-states. This is because the main contribution to the total fatigue damage in most cases comes from low- to moderate sea-states with high probability of occurrence rather than a few extreme sea-states. Compared to extreme response analysis, the degree of non-linearity involved is generally smaller. Adequate results can hence be obtained by use of linearized time domain- or frequency domain analyses in many cases. However, any use of simplified analysis methodology should be verified against nonlinear time domain analyses.

**Guidance note:**

Experience has also shown that, in a few cases in the Gulf of Mexico that the fatigue in the top part of an SCR is concentrated in the higher storms. Similar instances of significant contribution of fatigue damage from extreme seastates have also been observed in TTR's of the North Sea. Hence, extreme seastates should not be ignored with respect to fatigue, when blocking scatter diagrams.

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The fatigue damage will generally have contributions from wave frequency (WF)- as well as low frequency (LF) stress cycles. The WF floater motions as well as direct wave loading on the riser govern WF fatigue damage, while the LF floater motions govern LF fatigue damage. The relative importance of WF and LF fatigue damage is strongly system dependent and will in addition vary significantly with the location along the riser. It is always recommended to do an assessment of the relative contributions from WF and LF stress cycles to the fatigue damage to support rational decisions regarding choice of method of analysis. Methods for combining LF and WF fatigue damage are addressed in Section 5.3.

**2.5 Fatigue stress**

The stress to be considered for fatigue damage accumulation in a riser is the cyclic (i.e., time-dependent) principal stress.

Variation in pipe wall thickness over the design life of the riser system should be considered in long-term fatigue damage calculations (i.e. in-place, operational condition). An average representative pipe wall thickness,  $t_{fat}$ , may be applied in nominal fatigue stress calculations. The following approximation may be applied for a stationary corrosive environment:

$$t_{fat} = t_{nom} - 0.5 \cdot t_{corr} \tag{2.11}$$

where  $t_{nom}$  is the nominal (specified) pipe wall thickness and  $t_{corr}$  is the corrosion allowance. Wear can be usually treated as corrosion loss in the bore but it is often not uniform in the riser, and probably not uniform throughout the lifetime of the riser.

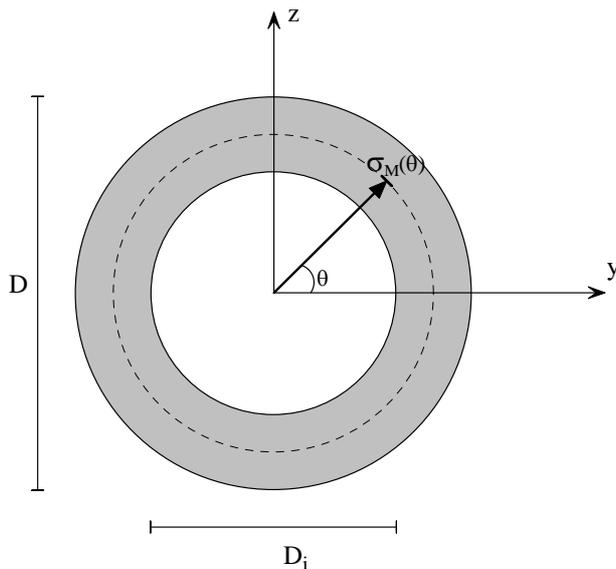
For fatigue damage calculations prior to permanent operation (e.g. tow-out, installation etc) the pipe wall thickness should be taken as:

$$t_{fat} = t_{nom} \tag{2.12}$$

The governing cyclic nominal stress component,  $\sigma$  for pipes is normally a linear combination of the axial and bending stress given by:

$$\sigma(t) = \sigma_a(t) + \sigma_M(\theta, t) \tag{2.13}$$

where  $\sigma_a(t)$  is the axial stress and  $\sigma_M(\theta, t)$  is the bending stress. The angular co-ordinate  $\theta$  gives the location of the hotspot along the circumference of the riser pipe.



**Figure 2-2**  
Calculation of bending stress

The axial stress  $\sigma_a$  can be written as:

$$\sigma_a(t) = \frac{T_e(t)}{\pi \cdot (D - t_{fat}) \cdot t_{fat}} \quad (2.14)$$

where  $D$  is the outer diameter of the metallic riser,  $T_e$  is the effective tension.

The bending stress at the mid-wall of the riser pipe, with reference to Figure 2-2, can be written as:

$$\sigma_M(\theta, t) = (M_y(t) \sin(\theta) + M_z(t) \cos(\theta)) \cdot \left( \frac{D - t_{fat}}{2I} \right) \quad (2.15)$$

where  $M_y$  and  $M_z$  are the bending moments about the local  $y$  and  $z$  axes respectively and  $I$  is the second moment of inertia.

This combined stress varies around the circumference of the riser pipe. For cases where the waves are incident from several different directions, the fatigue damage must be calculated at a number of regularly spaced points to identify the most critical location. It is recommended that at least 8 points along the circumference are used in the fatigue analysis.

Adequate fatigue life should be documented for all parts of the riser system. The fatigue criterion presented in Section 2.2 needs to be satisfied.

#### Guidance note:

Fatigues of flexible joints need particular attention. The designer should be aware of the sensitivities of flexible joint stiffness to both temperature and dynamic loading. Correct understanding of the flexible joint stiffness is important in determining the stresses and fatigue in the flexible joint location. Flexible joint stiffness for the large rotations which typically occur in severe storms is much less than for the small amplitudes occurring in fatigue analysis.

The fatigue assessment should also consider the criticality of the flexible joints components (bellows, bearings, elastomers, etc) and an assessment of long-term degradation is considered essential.

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## 2.6 Fatigue assessment by crack propagation calculations

A damage tolerant design approach applies. This implies that the riser components should be designed and inspected so that the maximum expected initial defect size would not grow to a critical size during service life or within the inspection interval. The inspection interval should be shorter than the duration for the crack to grow from a NDT detectable size to a critical crack size. Crack propagation calculations typically contain the following main steps:

- determination of long-term distribution of nominal stress range;
- selection of the appropriate crack growth law with appropriate crack growth parameters. Crack growth parameters (characteristic resistance) should be determined as mean plus 2 standard deviations;
- estimation of the initial crack size and geometry and/or any possible time to crack initiation. A flaw size that is larger than the sum of the NDT inspection threshold plus the sizing error of the NDT equipment, based on a qualified NDT procedure with 95% probability of detection, should be applied. Crack initiation time is normally neglected for welds;
- determination of cyclic stress in the prospective crack growth plane. For non-welded components the mean stress should be determined;
- determination of final or critical crack size (through the thickness, unstable fracture/gross plastic deformation);
- integration of the fatigue crack propagation relation with respect to the long-term stress range distribution to determine the fatigue crack growth life.

This Recommended Practice primarily focuses on the fatigue assessments based on the S-N curve approach. For detailed guidance on the fatigue analysis based on fracture mechanics approach, see BS 7910 "Guide on methods for assessing the acceptability of flaws in metallic structures".

## 3. S-N curves

### 3.1 General

The fatigue design can be based on use of S-N curves obtained from fatigues testing. For practical fatigue design, welded joints are divided into several classes, each with a corresponding S-N curve.

This Recommended Practice provides direct reference to the S-N curves given in the DNV-RP-C203. However it is acknowledged that an equivalent S-N curve, from the wide range of S-N curves, reported in the internationally recognised standards such as BS, HSE, API etc. may be adopted.

### 3.2 Two slope S-N curves: DNV-RP-C203

Fatigue capacity data for joint classifications of relevance for risers are given in DNV-RP-C203. The joint classifications which apply to typical joints/details for risers subjected to cyclic bending moment and tension are treated extensively in DNV-RP-C203.

If fatigue data does not exist for the material, detail and environment under consideration, S-N curves should either be developed by testing, use of fracture mechanics assessment or by conservative S-N curves. Special care should be taken for chemical environments not covered by DNV RP-C203.

The standard deviation of the log of the intercept of the S-N curve, i.e.  $\sigma_{\chi}$  is 0.20, for the two-slope S-N curves given in DNV-RP-C203.

### 3.3 Single slope S-N curves

The use a single-slope S-N curves based on the internationally

accepted standards, is considered acceptable. However, the recommendations given under Section 3.6 should be checked.

**Guidance note:**

A wide range of standard deviation of the log of the intercept of the S-N curve for the single-slope S-N curves is reported in the literature. For example the B curve can be associated with a  $\sigma_{x_a} = 0.18$  and the E curve can be associated with a  $\sigma_{x_a} = 0.25$  (see for example UK DOE 84). This  $\sigma_{x_a}$  value is provided as an input, when enhanced risk based safety factors are applied, instead of the standard safety factors. For more information, see Section 6.3.

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**3.4 Exceptions and cut off limits**

**3.4.1 Two slope S-N curves: DNV-RP-C203**

A detailed fatigue analysis can be omitted if the largest local stress range for actual details is less than the fatigue limit at  $10^7$  cycles for the 2-slope S-N curves given in DNV-RP-C203, for air and for seawater with cathodic protection. The effect of the Design Fatigue Factor (DFF) should be taken into account, by reducing the allowable stress range by a factor  $(DFF)^{-0.33}$ .

**Guidance note:**

The above mentioned clause for the omission of a detailed fatigue analysis requires that the largest local stress range for actual details is less than the fatigue limit at  $10^7$  cycles for the 2-slope S-N curves given in DNV-RP-C203, for air and for seawater with cathodic protection. The requirement that “the largest local stress range for actual details is less than the fatigue limit at  $10^7$  cycles” should be satisfied for all sea-states, during the lifetime of the riser.

If this requirement is violated even for a single sea-state, then a detailed fatigue analysis should be performed.

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It should be noted that the 2-slope S-N curves given in DNV-RP-C203, do not have any cut-off limits.

**3.4.2 Single slope S-N curves**

Some of the single-slope S-N curves, may have cut-off limits, i.e. the lower limit for the stress range. If the stress ranges are below the cut-off limit, its contribution to the fatigue damage is neglected.

**3.5 Stress concentration factor**

A stress concentration factor (SCF) applies to account for possible stress magnification due to imperfect geometry of two adjacent joints (e.g. due to fabrication tolerances and installation procedures at the welds or the mechanical connectors). The SCF may be calculated by detailed FE analyses or by closed form expressions for the actual structural detail. For hot spots where the stress is increased due to local bending resulting from an eccentricity, a stress concentration due to maximum allowable eccentricity should be included. This stress concentration factor can be assessed based on the analytical expression given in DNV-RP-C203.

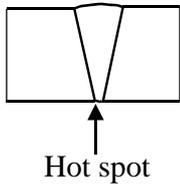
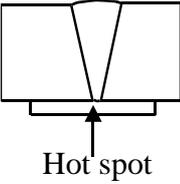
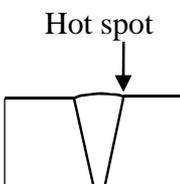
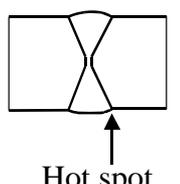
Assessment of the representative eccentricity should be based on detailed information regarding production tolerances and installation/welding procedures supported by rational conservative assumptions as appropriate for the actual design.

**3.6 Selection of S-N curves**

Fatigue crack initiation in homogenous materials will occur at the surface. Damages at the surface that can acquire stress concentration factor will thus shorten the life in the high-cycle fatigue regime. As long as surface damages in the base material remain small, the S-N curve applied for the welds will be dimensioning for equal load levels.

However, if the inherent surface damages in the base material are causing a stress concentration factor larger than the stress

range ratio between the S-N curve for the weld and the base material, the fatigue limit for the base material will be dimensioning. For this case, a new S-N curve for the base material with relevant surface defects should be established.

Welding	Geometry and hot spot	Location
Single side		Inner side of the riser
Single side on backing		Inner side of the riser
Single side		Outer side of the riser
Double side		Inner side of the riser

DNV-RP-C203, recommends specific S-N curves for risers and it is recommended that the relevant S-N curves should be selected based on:

- constructional details
- fabrication process – welded, clad, forged, machined, etc.
- base metal or weld
- welds - hotspots on the inner surface and outer surface
- weld details and tolerances, weld type (welding with or without backing, double sided weld)
- stress concentration factors from concentricity, thickness variations, out of roundness and eccentricity; angularity
- environment - air, free corrosion or cathodic protection in sea water.

For welded locations, the fatigue damage at the hotspot on the inner surface as well as the outer surface needs to be evaluated, using the appropriate SCFs for that location.

If riser collision is prevalent, then in the contact area, there may be local geometry deformation, which will influence the fatigue properties. It is accordingly recommended to generate a relevant S-N curve by testing for this section of the riser.

DNV-RP-C203, is recommended for guidance.

**3.7 Qualification of S-N curves**

Qualification of S-N curves is relevant in the following cases:

- S-N curve other than the ones recommended by interna-

tionally accepted standards is being applied. (e.g. S-N curves based on in-house fatigue testing)

- S-N curve with better fatigue properties, which is different from the recommended S-N curves (e.g. S-N E curve is applied at the root of the single sided weld instead of the applicable F1 curve as required by DNV-RP-C203).

**Guidance note:**

This guidance note is applicable for the use of the S-N curves given in DNV-RP-C203. Consider a case when the S-N E curve is applied at the root of the single sided weld instead of the applicable F1 curve as required by DNV-RP-C203. If the E curve is used, instead of the applicable F1 curve, it has to be ensured that defects in the root are not allowed in the fabrication process. This has to be verified by a suitable NDT method. An assessment of the NDT methodology needs to be performed to see if it is possible to go to a higher S-N curve than F1.

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In order to qualify a “less stringent” S-N curve the following work should be performed:

- assess maximum combined tolerances based on specification of fabrication of the riser
- perform fatigue testing of test specimens fabricated according to a proposed fabrication procedure. Strain gauges should be placed on both sides of the test specimens for measurements of bending stress over the thickness. The test specimens should be fabricated with maximum allowable tolerances
- the characteristic S-N curve for use in design is defined as the “mean-minus-two-standard-deviations” curve as obtained from a  $\log_{10}S\text{-}\log_{10}N$  plot of experimental data. With a Gaussian assumption for the residuals in  $\log_{10}N$  with respect to the mean curve through the data, this corresponds to a curve with 97.7% survival probability. The uncertainty in this curve when its derivation is based on a limited number of test data should be accounted for
- assess distribution of defects that may be present after NDT of the welded connections
- perform crack growth analysis based on fracture mechanics, on defect sizes that can be detected by NDT, to demonstrate that sufficient fatigue capacity is achieved
- it is recommended that an independent verification is performed by a competent verification body to qualify the S-N curve.

Further guidance on statistical study of fatigue data and establishment of S-N curves can be found in “Best Practice Guide on Statistical Analysis of Fatigue Data”, C. R. A. Schneider and S. J. Maddox Doc. IIW-XIII-WG1-114-03, February 2003.

## 4. VIV induced fatigue damage

### 4.1 VIV analysis

#### 4.1.1 General

Riser systems may experience Vortex Induced Vibrations (VIV). VIV may be split into:

- Cross-Flow (CF) vibrations with vibration amplitudes in the order of 1 diameter
- CF induced In-Line (IL) vibrations with amplitudes of 30-50% of CF amplitude and
- Pure IL VIV with amplitudes in the order of 10-15% of diameter.

For risers, pure IL is normally not considered.

The main effects of VIV of relevance to riser system design are:

- the riser system may experience significant fatigue due to VIV
- CF VIV may increase the mean drag coefficient to be applied in global load effect analyses and riser interference analyses
- VIV may influence Wake Induced Oscillations (WIO) of riser arrays (onset and amplitude)
- VIV may contribute significantly to the relative collision velocity of two adjacent risers (relevant only if structural riser interference is a design issue).

VIV evaluation is of particular importance for deep water top-tensioned risers and steel catenary risers.

#### 4.1.2 Fatigue analysis

VIV fatigue analyses may be carried out by state-of-practice tailor made software for engineering applications. The main features of such software are:

- semi-empirical parametric Cross-Flow VIV load/response formulation based on model test results
- linear structural model
- direct FD solution based on linearised dynamic equilibrium equations at static equilibrium position, or
- modal solution based on eigenmodes and eigenfrequencies computed from FE model of the riser system
- FD fatigue damage calculation.

**Guidance note:**

Detailed sensitivity studies should always be performed when VIV analysis is based on semi-empirical software. Some of the semi-empirical VIV software have a set of ‘user controlled input parameters’, which are purely related to the VIV modelling (and not the riser design). Significant variations can often be seen in the computed fatigue damage results, when the ‘user controlled input parameters’ are varied. The validity of the applied ‘user controlled input parameters’ should be realistic and justifiable. By means of sensitivity studies, non-conservative solutions should also be identified and avoided.

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The main limitations of the state-of-practice approach are:

- In-line VIV is ignored, which will generally give non-conservative fatigue damage estimates (especially if high in-line modes are excited by CF VIV).
- Linear structural model (e.g. constant effective tension) may give inaccurate results in e.g. touch-down area of SCR's.
- Axial stress due to cross-flow VIV is not included (this would require a NLTD VIV analysis).

Numerical TD simulation of the turbulent fluid flow around one- or several pipes can in principle be applied for VIV assessment to overcome the inherent limitations of the state-of-practice engineering approach. This approach is commonly termed 'Computational Fluid Dynamics'-CFD. The application of CFD for VIV assessment is at present severely limited by the computational efforts required as indicated in the following:

- 3D - CFD model linked to LTD or NLTD structural model. Presently not applicable due to the enormous computational efforts involved
- 2D - CFD approach (e.g. Navier Stokes or vortex in cell method) applied as strip model in a LTD or NLTD structural model. May be applied for verification of selected critical conditions of some riser systems. Demanding to apply for cases where high modes are excited by VIV (e.g. deep water risers) as a large number of strips will be needed to give an adequate load and response representation. If such an approach is adopted, CFD simulation should be performed using full scale Reynolds number with associated turbulence models when necessary

- 2D/3D CFD models applied for single/multiple cylinder sections with flexible supports. May be applied for screening purposes.

The numerical methods may be validated and calibrated against field measurements or laboratory tests. Particularly for novel design with which one has little experience, experiments should be considered.

**Guidance note:**

Often, the main design focus is to evaluate if the fatigue capacity is sufficient. Accordingly, a simplified conservative VIV analysis will suffice if the resulting fatigue damage is within the tolerated limit. If the simplified analysis indicates insufficient fatigue capacity, more sophisticated analyses should be applied. The method should be chosen according to the specific case investigated.

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**4.1.3 Screening for VIV**

For deepwater risers, a simplified estimate of the induced fatigue damage can be computed by neglecting the influence of the waves, assuming undisturbed current velocities to apply.

For screening purposes a 1-year velocity with associated velocity profile is considered conservative. Otherwise, a weighted summation of computed damage over the long-term current distribution for velocities and direction should be performed.

**4.1.4 Wave induced VIV**

See DNV-RP-C205, if the riser is in shallow water or when wave induced VIV may be relevant.

**4.2 Acceptance criteria**

The acceptance criteria for VIV fatigue damage can be written as:

$$D_{VIV} \cdot DFF_{VIV} \leq 1.0 \tag{4.1}$$

where

$D_{VIV}$  = Accumulated fatigue damage from VIV only.  
 $DFF_{VIV}$  = Design fatigue factor for VIV. Refer to Section 6.

The  $D_{VIV}$  is the accumulated fatigue damage over the life time of the riser, based on the long term current distribution. In addition to the accumulated fatigue damage due to VIV, short-term extreme VIV events, such as VIV fatigue due to 100 year currents, should be considered.

**Guidance note:**

For extreme events, such as a 100 year submerged or loop/eddy current the VIV fatigue analysis may be conducted separately from all other events. The robustness check criterion for such special events can be specified as follows:

$$D_{VIV-ST} \cdot DFF_{VIV-ST} \leq 1.0 \tag{4.2}$$

where

$D_{VIV-ST}$  = Fatigue damage from the extreme VIV event (Short term event)  
 $DFF_{VIV-ST}$  = Design fatigue factor for the extreme VIV event (Short term event). It is standard industry practice to use a value of 10.

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**4.3 Simplified assessment of fatigue damage**

A simplified method for estimation of both cross flow and in-line VIV damage is outlined in this section. In the narrow

sense, the procedure applies for top tensioned risers with uniform cross-section in unidirectional current dominated flow conditions. By using engineering judgement it may also be used for steel catenary risers (SCRs) and for risers with varying diameter. The procedure is particularly suited for risers responding at mode no. 3 and higher.

**Guidance note:**

This simplified method is a relatively transparent, semi-empirical method and it can be applied for screening purposes. However, it should be noted that the response amplitudes given in Figure 4-2 and Figure 4-3 are based on limited available data. It should be reiterated that the response amplitudes given in Figure 4-2 and Figure 4-3 are based on top tensioned risers with mass ratio range of 1.5 to 2.5 having uniform cross-section and exposed to unidirectional current.

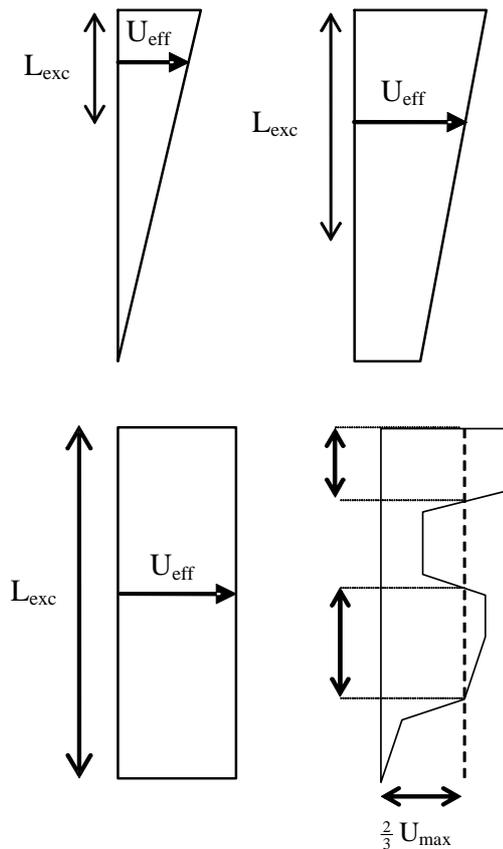
Hence case specific assessments should be made by the designer and caution and due diligence should be observed, before extrapolating this method to any general riser concept. Also, case specific assessments for the effect of damping, strakes, riser configuration and current environment, should be performed.

Further, the designer can evaluate and possibly increase the extent of conservatism in the screening method, if required.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

Identify participating mode shapes and natural frequencies as follows:

- determine the natural frequencies and mode shapes in the cross-flow and in-line directions based on analytical models or by numerical FEM analysis



**Figure 4-1**  
**General velocity profile where the excitation length is indicated (for the last case, the two parts are added together to obtain  $L_{exc}$ )**

- find a representative effective velocity and corresponding excitation length. When constant drag diameter is

assumed, the excitation length can be taken as the part of the riser where the velocity is larger than 2/3 of the maximum velocity as shown in Figure 4-1

- the effective velocity is the mean velocity over the excitation length.

$$U_{eff} = \frac{1}{L_{exc}} \int_{L_{exc}} U(z) dz \quad (4.3)$$

**Guidance note:**

If the calculated excitation length is less than 10% of the riser length, VIV may not be excited for this velocity. In such a case, the excitation length and effective velocity should be recalculated, after ignoring the contribution from the highest velocities in the profile.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

- Define a vortex shedding frequency  $f_s$  using:

$$f_s = S_{t,eff} \frac{U_{eff}}{D_h} \quad (4.4)$$

where  $U_{eff}$  is the effective flow velocity and  $D_h$  is the hydrodynamic outer diameter.  $S_{t,eff}$  is the effective Strouhal number for oscillating cylinders (typically  $S_{t,eff} = 0.17$  to  $0.25$ ).

- All modes with a natural frequency within a frequency band around  $f_s$  are assumed to be excited. The frequency band for cross-flow and in-line is respectively

$$f^{CF} \in [(1 - \Delta)f_s, (1 + \Delta)f_s] \quad (4.5)$$

$$f^{IL} \in [(1 - \Delta)2f_s, (1 + \Delta)2f_s] \quad (4.6)$$

Recent experiments have shown that the frequency bandwidth parameter  $\Delta$  can vary over the range from 0.10-0.25.

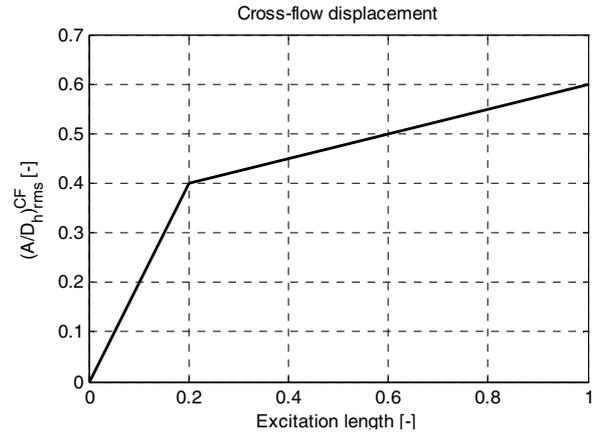
**Guidance note:**

When the eigen frequencies are close, priority may be given to the modes in the middle of the frequency band and a low bandwidth parameter can be selected. When the eigen frequencies are farther apart (lower modes) it may be necessary to use a larger bandwidth parameter, to ensure that at least one mode is selected.

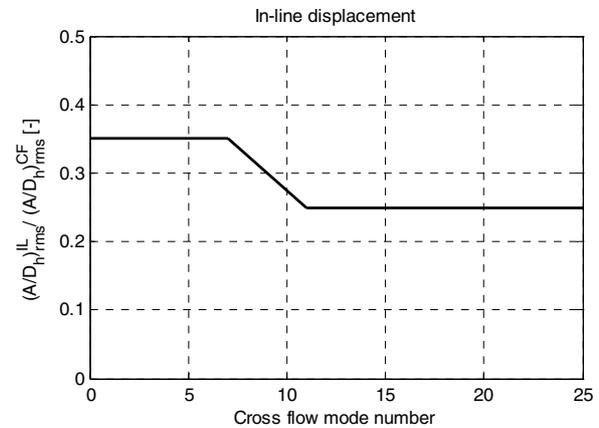
---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

The rms value of the cross-flow response amplitude is found from the excitation length according to Figure 4-2. The rms amplitude value is defined as the average rms amplitude over the riser span, assuming that the modes are non-stationary. This implies that equal amplitude is applied along the entire riser span.

The rms value of the in-line VIV response amplitude is found as a fraction of the cross-flow rms amplitude, dependent on the mode number for cross-flow response according to Figure 4-3.



**Figure 4-2**  
 $(A / D_h)_{rms}^{CF}$  as a function of excitation length



**Figure 4-3**  
 $(A / D_h)_{rms}^{IL} / (A / D_h)_{rms}^{CF}$  as a function of cross-flow mode number

Fatigue damage can be calculated for cross-flow and in-line VIV separately. All excited modes can be taken to give equal contribution to the rms amplitude. The amplitudes for the separate modes are thus found as:

$$a_{i,rms}^{CF} = \frac{1}{\sqrt{p^{CF}}} A_{rms}^{CF} \quad (4.7)$$

$$a_{i,rms}^{IL} = \frac{1}{\sqrt{p^{IL}}} A_{rms}^{IL} \quad (4.8)$$

where  $A_{rms}$  is the total rms amplitude,  $a_{i,rms}$  is the rms amplitude for mode number  $i$ , and  $p$  is the number of contributing modes. Superscript CF denotes cross-flow and IL denotes in-line.

**Guidance note:**

The above formulation assumes that all excited modes can be taken to give equal contribution to the rms amplitude. However, based on physical evidence or as a part of the sensitivity studies, it is recommended that weighted contribution (i.e. not equal contribution) from the different participating modes should also be considered.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

The corresponding stress standard deviation is computed as:

$$\sigma_S = SCF \cdot E \cdot \kappa_{eff} \cdot \frac{1}{2} (D_s - t_{fat}) \quad (4.9)$$

using

$$\kappa_{eff} = \sqrt{\sum_i (\kappa_{i,max} a_{i,rms})^2} \quad (4.10)$$

where  $E$  is the modulus of elasticity and SCF is a stress concentration factor.  $\kappa_{i,max}$  is the maximum curvature of mode shape  $i$ .  $D_s$  is the strength diameter (outer steel diameter) and  $t_{fat}$  is the fatigue thickness of the riser.

The representative maximum fatigue damage for the riser is estimated by application of the relevant S-N curve together with the above defined stress standard deviation as described in Section 2.2. It is recommended that the maximum fatigue damage is used as representative fatigue damage in the screening criteria, if this simplified approach is adopted. The frequency can be taken as the Strouhal frequency defined in Equation (4.4) for CF and twice the Strouhal frequency for IL.

**Guidance note:**

The inline response does not always occur at twice the cross-flow response frequency. In some of the test cases and full scale measurements, the ratio of the inline response to cross-flow response frequency varies in the order of 1 to 3. However, as an engineering simplification, the inline response is assumed to occur at twice the cross-flow response frequency.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

**4.4 Methods for mitigation of VIV**

If the calculated VIV-response is a problem, improvements can be achieved by:

- modifying the properties of the riser, i.e., tension, diameter, structural damping
- introducing vortex suppression devices
- model testing verifications.

**4.4.1 Vortex suppression devices**

Zdravkovich (1981) classifies the means of suppression to three categories according to the way it influences the vortex shedding:

- surface protrusions (wires, helical strakes etc.) triggering separation
- perforated shrouds, axial slats etc. (breaking the flow into many small vortices); and
- near wake stabilisers, preventing the building of the vortex street.

In Blevins (1990), eight different devices are shown, and comments on their use and effects are given. Common for all (except the ribboned cable) is that they increase the cost of the riser, and that they will complicate handling during installation. Some of the devices also reduce the drag coefficient, especially the streamlined fairing.

The most commonly used vortex suppression devices are helical strakes. Their function is to trigger separation in order to decrease the vortex shedding correlation along the riser. They increase the cost of the riser, and they will complicate handling during installation. The in-line drag coefficient is increased by introducing strakes. The helical strakes can be substantially reduced in their effectiveness for downstream risers.

**4.4.2 Qualification of vortex suppression devices**

The important parameters for the strake design are the height and pitch of the helical strakes for a given riser diameter. The overall performance characteristics of a given strake design will vary with the current velocities.

**Guidance note:**

Recent model tests and full scale tests with straked risers, where DNV has been involved, indicate a varying degree of suppres-

sion effectiveness.

In the engineering practice, often a reduction of the non-dimensional response amplitude ( $A/D$ ) is applied to model the effect of the strakes, in the riser VIV analysis. For example an 80% reduction in  $A/D$  is applied to quantify the suppression effectiveness in riser VIV analysis and this may not be a conservative estimate, e.g. for riser arrays. The  $A/D$  reduction factor will vary with the current velocity, excited mode number, cross-flow or inline response etc.

If the suppression effectiveness can be appropriately quantified and based on case specific and previously qualified or validated results, then the approach is considered acceptable.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

The effectiveness of VIV suppression devices, such as VIV strakes needs to be qualified. It is recommended that an independent verification of the effectiveness of VIV suppression devices is performed by a competent verification body. The third-party verification and approval should consider the following, for a given strake design:

- model test results with and without strakes
- effect of hydrodynamic scaling
- range of current velocities and associated efficiency
- durability and impact assessments
- effect of marine growth
- effect of surface finish.

**Guidance note:**

The model test results may be supplemented by CFD assessments, if available. However, a qualification based purely on CFD analysis is not considered acceptable.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

**4.4.3 Simplified VIV analysis for riser with strakes**

The simplified VIV analysis described in Section 4.3 can be modified to account for VIV mitigation due to strakes. The effectiveness of the strakes for the riser configuration should in general be qualified according to Section 4.4.2. In the absence of more accurate data, the following methodology can be used for estimation of VIV response for two specific strake configurations. These strakes have a triple start helical configuration.

- Pitch of 17.5 D and strake height of 0.25 D
- Pitch of 5 D and strake height of 0.14 D.

A modified excitation length is calculated according to the simplified method in Section 4.3 by considering only the part of the current velocity profile where the riser is bare. This means that for calculation purposes, the velocity is set to zero at straked parts of the riser in the excitation length calculation. The effective velocity and thereby the vortex shedding frequency of a partly straked riser in sheared flow can be estimated by averaging the velocity over the modified excitation length.

The VIV cross-flow amplitude can be found from Figure 4-2 using the modified excitation length and multiplied with an additional reduction factor. The VIV amplitude reduction factor is dependent on the strake coverage over the riser length. The following amplitude reduction can be used for the two above-mentioned strake configurations;  $(1 - \alpha^2)$ , where  $\alpha$  is the strake coverage fraction. For sheared flow, the reduction factor assumes strakes at the position of the highest current velocities.

**Guidance note:**

Coverage fraction  $\alpha > 0.9$  is not considered achievable in practice, corresponding to an amplitude reduction factor of 0.2.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

For a fully straked riser (coverage in excess of 80%), in-line VIV can be neglected. For smaller coverage ratios, the in-line VIV amplitude is calculated from the cross-flow VIV amplitude according to Figure 4-3.

## 5. Combined Fatigue Damage

### 5.1 General

Recommended procedures for short-term fatigue damage calculation for commonly used global analysis strategies are given in DNV-OS-F201. The following section presents methods for computing the combined fatigue damage from different constituent processes.

### 5.2 Acceptance criteria

The fatigue life may be calculated based on the S-N fatigue approach under the assumption of linear cumulative damage (Palmgren-Miner rule).

The acceptance criteria for the combined fatigue damage can be written as:

$$D_{\text{fat}} \cdot \text{DFF} \leq 1.0 \quad (5.1)$$

where

$D_{\text{fat}}$  = Accumulated fatigue damage (Palmgren-Miner rule), which includes fatigue damage from WF, LF and VIV.

DFF = Design fatigue factor, see Section 6

### 5.3 Combining WF and LF fatigue damage

The following methods can be applied to compute the combined fatigue damage from the WF and the LF fatigue components. The methods are presented in the order of preference.

#### 5.3.1 Time domain with RFC

The combined WF and LF motions can be used and the resulting stress history can be generated. The fatigue damage may be obtained by counting the stress cycles in the actual or simulated stress time histories. Special purpose counting algorithms have been developed with techniques applicable to non-Gaussian stress time histories. The recommend method is the Rain Flow Counting (RFC) method.

However, simplified and faster methods may be used during FEED phase, feasibility checks or parameter studies. In the Detailed Engineering or the EPC Phase, it is recommended that “Non-linear time domain method with RFC” is applied. It can also be used in combination with simplified methods to benchmark / confirm that the applied simplified methods are conservative.

See Appendix A, Section A.2.2 titled “Cycle counting”, for further information.

#### 5.3.2 Single moment method

The single moment method of Lutes and Larsen (1991) is a correction to the Rayleigh method, (see Section 5.3.4). Various correction factors are proposed in the literature, such as in Wirsching and Light (1980), Ortiz and Chen (1987), Lutes and Larsen (1990, 1991).

The single moment method of Lutes and Larsen (1991) gives more accurate results, as compared to the Rayleigh method. This approach is computationally simple and gives robust results and is hence recommended.

See Appendix A, Section A.2.3 titled “Semi-empirical solutions”, for further information.

#### 5.3.3 Bimodal method

Accurate analytical solutions to fatigue damage estimates can be obtained for well-separated bi-modal stress spectra (e.g. a process with a combination of low frequency and wave frequency Gaussian component). See Jiao & Moan (1990), where a correction function has been derived by analytical means assuming two independent narrow-banded Gaussian proc-

esses. See also Appendix A, Section A.2.4 titled “Analytical Solutions for Bi-modal Spectra”, for further information.

#### 5.3.4 Rayleigh method

The stress peaks from a combined analysis are assumed to be Rayleigh distributed.

#### 5.3.5 Simplified method

When a detailed stochastic analysis is performed for each of the dynamic processes, a combined stress response can be calculated, before the S-N curve is entered and the fatigue damage is calculated. The methodology is based on information of mean zero up-crossing frequency in addition to the calculated fatigue damages for each of the processes. The simplified approach is described in Appendix A Section A.3 and gives conservative results.

#### 5.3.6 Direct summation

The WF and LF fatigue damage criteria are calculated separately then summed directly to obtain the total fatigue damage. This method is least preferred, since simply adding up separately computed damage criteria based on narrow-banded assumptions can be ineffective.

### 5.4 Combining with VIV fatigue damage

It is standard industry practice to sum up the VIV fatigue damage with the combined WF+LF fatigue damage. This procedure is acceptable, as long as the critical locations for the VIV fatigue damage are not the same as the critical locations of the combined WF+LF damage.

The “Simplified DNV method” given in Section 5.3.5 can also be conservatively used to combine the VIV fatigue damage with the WF+LF damage. This “Simplified DNV method” can also be adopted even if critical VIV and WF + LF locations are the same.

## 6. Design Fatigue Factors

### 6.1 General

The safety factors for fatigue are often referred to as the Design fatigue factors (DFF) and are applied to increase the probability for avoiding fatigue failures.

The DFFs are dependent on the significance of the structural components with respect to structural integrity and availability for inspection and repair. DFFs shall be applied to the service life and the calculated fatigue life shall be longer than the product of the service life and DFF.

A key issue is to obtain an acceptable failure probability for FLS either by use of rational fatigue safety factors or by direct application of a reliability based approach. An approach with increasingly advanced methods of evaluation is recommended and should be chosen based on the guidance given below:

- 1) Safety factors in compliance with DNV-OS-F201, for wave induced fatigue. For further details, see Section 6.2. Fatigue safety factor for VIV induced fatigue is presented in Section 6.5.
- 2) Risk based safety factors selected based on a set of parameter studies. This methodology is described in Section 6.3.
- 3) Safety factors obtained from case specific Structural Reliability Analysis. For further details, see Section 6.4.

For traditional riser concepts known to possess acceptable reliability, the standard design fatigue factors, as given in Section 6.2 can be applied. In case of fatigue designs on the limit and for novel concepts, the risk based riser fatigue criterion is highly relevant. A case specific SRA is relevant for probabilistic design cases.

## 6.2 Standard design fatigue factors

The standard DFF is applicable to traditional riser concepts known to have adequate reliability. The standard DFF given in Table 6-1 is applicable for steel risers.

Table 6-1 Design fatigue factors DFF		
Safety class		
Low	Normal	High
3.0	6.0	10.0

### Guidance note:

The standard DFF is recommended ONLY for the traditional riser concepts, known to have adequate reliability. For the traditional riser concepts, with fatigue limit being the governing criteria and when the calculated fatigue life is close to the target fatigue life, the application of standard DFF needs to be evaluated. The following two options are recommended:

- 1) It is recommended that the sensitivity studies are conducted and the fatigue acceptance criterion is checked, for all possible parametric variations that can occur during the lifetime of the installation. Perform design changes, if the fatigue criterion is violated.
- 2) Alternatively, the risk based fatigue safety factors can be evaluated, as described in Section 6.3.

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## 6.3 Enhanced risk based safety factors

### 6.3.1 General

An optimal fatigue safety factor to be used in design can be calculated accounting for the uncertainty in the fatigue estimate. The design format proposed herein is calibrated to acceptable failure probabilities using structural reliability methods. The design approach, however, does not apply reliability methods. A set of dedicated analyses is performed for the prevailing uncertain input parameters in order to estimate the resulting uncertainty in the fatigue damage (or equivalent fatigue life estimate), [refer Mørk et al. (2002)]. An a priori reliability based fatigue safety factor calibrated corresponding to recognized safety levels can then be selected, as given in equation (6.3).

The methodology given in this section has been successfully benchmarked versus level III reliability analysis, for a significant number of floater-riser concept combinations and different environmental conditions, [refer Chezian et al. (2003)].

### 6.3.2 Assumptions

The equation for the safety factors are based on the variability in the fatigue life capacity due to variability in the prevailing uncertainty for the riser concept. The following assumptions apply

- The safety factors are calibrated apriori' using structural reliability analysis (SRA) implicitly accounting for the uncertainty in the Miner sum, S-N curves and variability in fatigue estimate. However, SRA is not required for the design analysis procedure.
- It is assumed that the basic variables are independent.
- Multiple slope S-N curves are implicit in the formulation and the uncertainty in the S-N curves is given as input in the safety factor computation.
- The implicit uncertainty is handled by a safety factor,  $\gamma$ , while the implicit bias is handled via an auxiliary correction factor,  $\alpha$ .
- Safety factors can be established from a limited set of "standard" fatigue parameter studies in the most damaging

sea-states, i.e. seastates that contribute significantly to the fatigue damage.

### 6.3.3 Design format

The objective is to establish a generally applicable design criterion with the ability to provide fatigue designs with a uniform safety level in compliance with DNV-OS-F201. The enhanced fatigue design criterion for a bi-linear S-N curve can be formulated as:

$$D_{fat}(T) = \frac{T f_v}{\bar{a}_1} \left[ \int_{S_{sw}}^{\infty} s^{m_1} f_S(s) ds + \frac{\bar{a}_1}{\bar{a}_2} \int_0^{S_{sw}} s^{m_2} f_S(s) ds \right] \leq \frac{\alpha}{\gamma} \quad (6.1)$$

where,

- $\alpha$  = bias factor
- $\gamma$  = fatigue safety factor
- $f_v$  = mean frequency of stress cycles
- $f_S(s)$  = probability density function for the stress cycles
- $m_1, m_2$  = fatigue exponents
- $\bar{a}_1, \bar{a}_2$  = characteristic fatigue strength constants

- $S_{sw}$  = stress at intersection of the two S-N curves
- $T$  = design life time in years

The bias factor  $\alpha$  accounts for the bias, i.e. expected systematic deviation from the 'true' fatigue damage obtained by the applied fatigue analysis methodology.

$$\alpha = \frac{D_{fat}(applied\ method)}{D_{fat}(state\ of\ the\ art\ method)} \quad (6.2)$$

### Guidance note:

For example the Frequency Domain (FD) approach can be the 'applied method', and the 'state-of-the-art method' can be a Time Domain (TD) solution with a Rain Flow Counting (RFC) technique.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

The fatigue safety factor,  $\gamma$ , is based on prior calibration, using structural reliability analyses. It is given in the form

$$\log_{10} \gamma = (30 + \gamma_{SC}) T^{a(30 + \gamma_{SC}) + b} (c \sigma_{X_D} + d) (\sigma_{X_a})^{(e \sigma_{X_D} + f)} \quad (6.3)$$

where,

- $\gamma_{SC}$  = safety class factor accounting for the failure consequence
- $\sigma_{X_D}$  = uncertainty in fatigue damage
- $\sigma_{X_a}$  = uncertainty in normalized fatigue constant on a log scale
- $T$  = design life time in years
- $a, b, c, d, e, f$  = coefficients as defined in Table 6-3.

The acceptable failure probabilities (annual per riser) are taken in compliance with DNV-OS-F201, and the numerical values approximately represent a step of a decade in failure probabilities. The associated Safety Class Factors are presented in Table 6-2.

Low	Normal	High
( $P_f < 10^{-3}$ )	( $P_f < 10^{-4}$ )	( $P_f < 10^{-5}$ )
2	7	10

The term  $T^{a(30+\gamma_{SC})+b}$  in (6.3) is a correction factor for the effect of the design life since the acceptance criteria (in terms of acceptable failure probability) has been expressed in terms of annual failure probability rather than lifetime failure probability.

**Guidance note:**

Hence, designing for a given acceptable failure probability e.g. from year 9 to year 10 requires a slightly higher safety factor than from year 39 to year 40.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

The coefficients ‘a, b, c, d, e, f’ given in equation (6.3) are defined in Table 6-3 and have been calibrated for two specific ranges of  $\sigma_{X_D}$ , i.e. for  $0.1 < \sigma_{X_D} < 0.3$  and  $0.3 < \sigma_{X_D} < 0.5$ , where  $\sigma_{X_D}$  is the uncertainty in fatigue damage.

Coefficient	$0.1 < \sigma_{X_D} < 0.3$	$0.3 < \sigma_{X_D} < 0.5$
a	0.0205	0.0181
b	- 0.8998	- 0.8049
c	0.0218	0.0730
d	0.0242	0.0084
e	- 1.2802	- 0.1711
f	0.2894	- 0.0445

The uncertainty in the fatigue damage represented by  $\sigma_{X_D}$  is case specific and needs to be established by a few standard fatigue analyses.

The normalized fatigue utilization,  $X_D$  is defined by:

$$X_D = \log \left( \frac{D_s(x)}{D(\mu_x)} \right) \quad (6.4)$$

$D_s(x)$  is the stochastic fatigue damage estimated from (6.1), i.e., with stochastic parameters in the stress range distribution due to uncertainty in case specific governing random variables,  $X$ .  $D(\mu_x)$  is the corresponding basic fatigue damage with deterministic (best estimate or mean value) parameters normally applied in fatigue design analyses.

The 1<sup>st</sup> order approximation to the standard deviation  $\sigma_{X_D}$  can be expressed as:

$$Var[X_D] = \nabla^T C \nabla \quad (6.5)$$

where  $C$  is the covariance matrix of  $x$  (i.e.  $C_{ij} = cov(X_i, X_j)$ ) and  $\nabla^T$  is the gradient vector of  $X_D$  with respect to  $x$  evaluated at the mean value  $\mu_x$ . For un-correlated random variables this expression simplifies to:

$$\sigma_{X_D} = \sqrt{\sum \left( \frac{\partial X_D}{\partial x_i} \right)^2 \sigma_{x_i}^2 + \sigma_{X_{mod}}^2} \quad (6.6)$$

$X_{mod}$  is a model uncertainty accounting for the uncertainty sources not accounted for in the assessment of  $X_D$  and reflects the confidence in the global analyses tools versus true life.  $X_{mod}$  is assumed to be unbiased with a standard deviation  $\sigma_{X_{mod}}$ .

**Guidance note:**

$X_{mod}$  is assumed to be unbiased with a standard deviation  $\sigma_{X_{mod}}$  which is typically in the order 0.05-0.10 and constitutes a lower bound for the total safety factor. It is recommended that  $\sigma_{X_{mod}}$  is not chosen smaller than 0.05. Note that any bias from analysis assumptions, models and tools is imbedded in  $\alpha$  factor of equation (6.1).

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

The term  $(\sigma_{X_a})^{(e\sigma_{X_a}+f)}$  in equation (6.3) accounts for the natural variability for the S-N curve,  $\sigma_{X_a}$ , where  $X_a$  is the normalized fatigue constant on a log scale.

**Guidance note:**

The  $\sigma_{X_a}$  of 0.20 is applicable for the 2-slope S-N curves given in the DNV-RP-C203. However, a wide range of  $\sigma_{X_a}$  is reported in the literature for the single slope S-N curves, ranging from 0.18 to 0.25. The modern lay barge welds may be qualified to have a much smaller  $\sigma_{X_a}$ , i.e., close to 0.15.

However, smaller  $\sigma_{X_a}$  can only be used provided that specific documentary evidence, relevant to the project is presented, to justify it.

---e-n-d---of---G-u-i-d-a-n-c-e---n-o-t-e---

The fatigue safety factor,  $\gamma$  is illustrated in the Appendix A Section A.5, as function of the uncertainty in the fatigue estimate  $\sigma_{X_D}$ , fatigue design life and safety class.

**6.3.4 Step by step procedure**

The enhanced risk based fatigue model essentially needs the probabilistic model of the basic input variables and response surfaces based on a few standard parametric studies.

The analysis procedure for riser applications is presented as a flow chart in Figure 6-2. Detailed description of the individual steps that are applied in the computation are given below:

- 1) Perform the standard riser fatigue analysis using best estimate design parameters. Often the fatigue analysis is carried out using the standard design parameters, as given in the project specific design basis. This will be referred to as the base case and the associated fatigue damage is denoted  $D(\mu_x)$ .
- 2) The accumulated fatigue damage  $D(\mu_x)$  has contributions from the various short-term seastates. Identify the most damaging short-term seastates, i.e. the seastates that contribute significantly to the total fatigue damage,  $D(\mu_x)$ . For example the top three most damaging seastates can be short-listed for performing the sensitivity studies.
- 3) Identify stochastic variables  $X_i$  governing the uncertainty in the fatigue damage estimate. The stochastic variables involved can normally be considered as un-correlated. Examples of stochastic variables include the drag coefficient, floater motions, floater offset, soil model uncertainty, soil stiffness, etc. A more detailed listing is presented in 6.3.5. The stochastic variables, being case specific, should at least include the variables listed in Table 6-4, but need not be limited to them. This implies that additional stochastic variables which may be relevant for the specific case must be included by designer.
- 4) Establish the probabilistic model (mean,  $\mu_{X_i}$  and standard deviation,  $\sigma_{X_i}$ ) of all stochastic variables involved, based on literature, experience and possible analysis of new data. Refer to 6.3.5 for a detailed listing of stochastic variables. Typically the mean values for the stochastic variables are chosen as the “base case” design parameters, as given in the project specific design guideline and design basis. The standard deviation,  $\sigma_{X_i}$ , should be based on project specific design information. However, if adequate information is not available for the standard deviation, the guidelines given in 6.3.5 can be used as an alternative.
- 5) Perform standard fatigue sensitivity studies for these sto-

chastic variables for the main sea states identified in step 2. The sensitivity studies should cover the parametric variation in the range of the two standard deviations on either side of the mean value ( $\mu_{X_i} \pm 2\sigma_{X_i}$ ). The fatigue damage for the sensitivity cases is denoted  $D_s(x)$ .

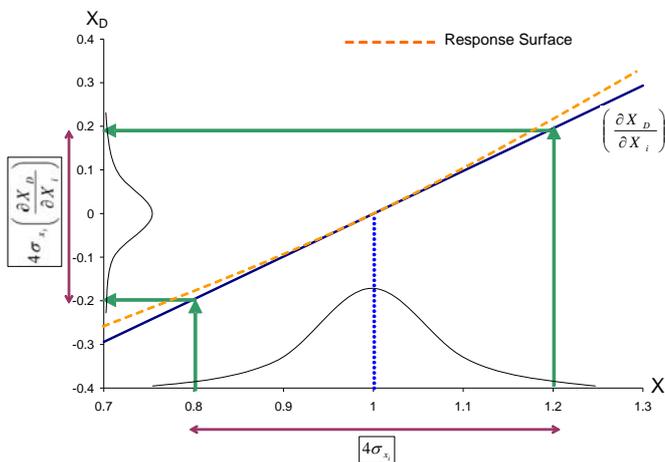
- 6) Generate the response surfaces based on results from step 5. The response surfaces are generated using equation (6.4), where the  $X_D$  forms the ordinate and the  $X_i$  values forms the abscissa. (see Figure 6-1.)
- 7) Establish numerical approximations to the partial derivatives  $\partial X_D / \partial X_i$ . This is in practical computations found as  $\Delta X_D / \Delta X_i$  where  $\Delta X_D$  is the change in  $X_D$  due to a prescribed increment  $\Delta X_i$  of the stochastic variable  $X_i$ . This is depicted in Figure 6-1, for a given response surface.

**Guidance note:**

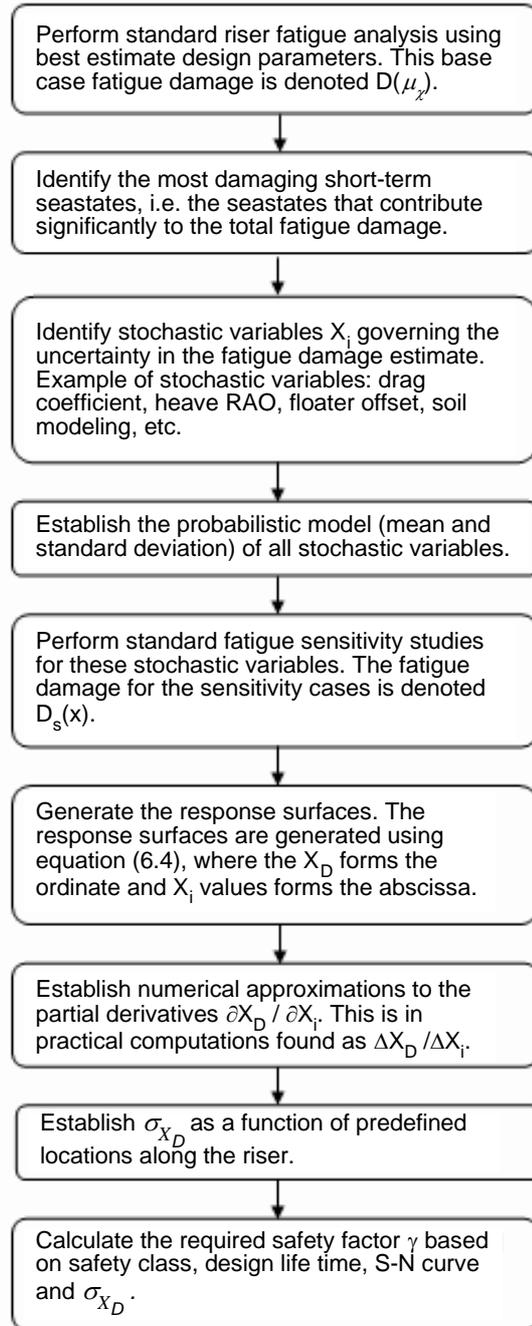
The above mentioned approach is in a narrow sense only valid, if the variability in the fatigue life capacity due to variability in the basic variables is linear or close to linear on a log scale. If the variability in the fatigue life capacity due to variability in the basic variables has a non-linear behaviour, then alternative methods should be adopted. One possibility is to estimate the contribution to the  $\sigma_{X_D}$  by mapping the probabilities of the variable  $X_i$  to the probabilities of the normalized fatigue utilization  $X_D$ , through the non-linear response curve.

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- 8) Establish  $\sigma_{X_D}$  as a function of predefined locations along the riser (e.g. using 's' as length co-ordinate along the riser and  $\theta$  as a circumferential location for given 's') using the 1<sup>st</sup> order approximation as in equation (6.6).
- 9) Calculate the required safety factor  $\gamma(s, \theta)$  using equation (6.3), based on safety class, design life time, S-N curve and  $\sigma_{X_D}$ . The safety class is discussed in Section 1.4 and the S-N curve parameter  $\sigma_{X_i}$  (standard deviation of the log of the intercept of the S-N curve) is discussed in Section 6.3.3.



**Figure 6-1**  
From response surface to  $\partial X_D / \partial X_i$



**Figure 6-2**  
Step-by-step procedure

**Guidance note:**

The fatigue safety factor is location specific, i.e. they vary along the length and periphery of the riser. Hence a single safety factor should not be applied for the entire riser length.

Consider a simplified example case, where at two locations of the riser the fatigue life and corresponding safety factors are estimated. The intended service life is 20 years.

- Fatigue life estimated fatigue life at location 1 is 210 years and the estimated fatigue safety factor is 12.
- Fatigue life estimated fatigue life at location 2 is 120 years and the estimated fatigue safety factor is 5.

The above example illustrates that at location 1, the acceptance criterion is not met. At location 2, the acceptance criterion is satisfied, despite the low fatigue life.

The above example also illustrates:

- it is not appropriate to use the safety factor which is found to

be highest for the whole riser configuration for subsequent fatigue design

- it is not appropriate to choose the riser section which is the most critical with respect to fatigue damage, and estimate the design factor for that cross-section
- hence it necessary to estimate the fatigue safety factor at all locations.

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### 6.3.5 Stochastic variables

As stated in Section 6.3.4, the stochastic variables  $X_i$  governing the uncertainty in the fatigue damage estimate needs to be identified. The stochastic variables involved can normally be considered as un-correlated. Different stochastic variables may be relevant based on the floater type, riser type, environmental modelling, analysis methodology etc. The stochastic variables, being case dependent, should at least include the variables listed in Table 6-4, whenever relevant, but need not be limited to them. This implies that additional stochastic variables which may be relevant for the specific project case, must be identified and included.

The probabilistic model (mean and standard deviation) of all stochastic variables involved should be based on literature, experience and possible analysis of new data. The “base case” design parameters, as given in the project specific design guideline and design basis, are typically chosen as the mean values for these stochastic variables. The standard deviation should be based on project specific design information. However, if adequate information is not available for the Coefficient of Variation (COV) of the stochastic variables, the indicative values listed in Table 6-4 may be used.

Table 6-4 Stochastic variables		
Variable	Probabilistic distribution	$COV = \frac{\sigma_{x_i}}{\mu_{x_i}}$
Drag coefficient	Lognormal	0.15 – 0.20
Floater RAO amplitude	Lognormal	0.05 – 0.10
Static Floater Offset	Normal	1% of water depth
Soil Stiffness	Lognormal	0.20 – 0.50
Soil riser interaction model uncertainty (trenching/suction effects)	Lognormal	0.10 – 0.30
Riser weight	Normal	0.05 – 0.10
Environmental modelling	Lognormal	0.05 – 0.10
Additional stochastic variables may also be relevant and need to be identified for each specific project.		

In addition to the stochastic variables that are listed in Table 6-4, the following (non-exhaustive) list of variables should be considered when relevant:

- corrosion
- flaring angle
- top angle
- floater heading
- internal fluid content
- thermal expansion
- marine growth
- inertia coefficient.

### 6.3.6 Relative importance factors

The safety factor assessment should be followed by a relative importance factor assessment. The different stochastic variables will usually have a varying degree of importance and provide a varying degree of contribution to the uncertainty estimate and hence to the safety factor.

The relative importance of the uncertainty associated with each of the basic variables, is of great practical significance.

The following possibilities can be envisaged:

- identify the key variable(s) with high relative importance (i.e. which contribute significantly to the  $\sigma_{X_D}$ ) and make design changes, which can minimize the influence of this key variable
- set focus on the key variable(s) and reduce/refine the level of uncertainty associated with this key variable, if possible. Reassess the required safety factor based on refined input
- monitor the key variable, over a period of time, to check if the applied uncertainty levels are not exceeded
- verify the safety factor estimation. For example, any unduly large contribution or unrealistic relative importance factor can imply that there is a possibility for an error in the input, probabilistic modelling or the computation.

The relative importance factor can be assessed by assuming the stochastic variables to be uncorrelated and comparing the relative percentage contribution to the total uncertainty.

$$I_{factor} = \frac{\left(\frac{\partial X_D}{\partial X_i}\right)^2 \sigma_{x_i}^2}{\sigma_{X_D}^2} \quad \text{for stochastic variables} \quad (6.7)$$

$$I_{factor} = \frac{\sigma_{X_{mod}}^2}{\sigma_{X_D}^2} \quad \text{for model uncertainty} \quad (6.8)$$

The relative importance factor should be calculated for all the variables and also the model uncertainty component. The relative importance factor will typically vary along the length of the riser and hence should be assessed at all critically utilised locations.

#### Guidance note:

To illustrate the significance of the relative importance factor the following examples are considered:

Example 1: “Soil stiffness” is the key variable, which contributes significantly to the  $\sigma_{X_D}$ . The uncertainties related to “soil stiffness” can be improved, by performing additional geo-technical studies, based on more accurate probabilistic modelling and case specific soil sampling/data. The refined soil data and the associated uncertainty in fatigue damage, can be used to re-estimate the required safety factors.

Example 2: “Heave motion” variable is contributing significantly to the uncertainty in fatigue damage. In this case, subtle floater design modifications can be considered, to minimise the heave motions and the associated uncertainty.

Example 3: “S-N Curves” is the key variable, which contributes significantly to the  $\sigma_{X_D}$ . Improvements can be achieved by adopting a better weld quality, grinding, cathodic protection etc.

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When the relative importance factor is assessed for numerous riser-floater concepts and environmental combinations, certain trends and relations emerge about the stochastic variables which are governing or dominating for each of the riser-floater-environmental combinations. However, it is advisable not to draw any general conclusions, based on a limited number of case studies. It is suggested that case specific and riser location specific assessments of the relative importance factor should always be done.

### 6.3.7 Qualification of safety factors

To avoid potential misinterpretations of this Recommended Practice or possible incorrect usage of the enhanced risk based safety factors, it is strongly recommended that an independent verification of the enhanced risk based safety factors is performed by a competent independent verification body.

The designer should ensure that the assessment of enhanced risk based safety factors is performed as per the specifications

given in Section 6.3.

The third party verification requirement is imposed to preclude any unintentional errors in the computation of the safety factors based on the novel approach specified in Section 6.3.

The third party verifier should *verify* that the calculated enhanced risk based safety factors are in full compliance with the requirements given in this Recommended Practice. The third party verifier should *verify* if the estimated enhanced risk based safety factors are consistent with the fatigue calculation approach and adequate target safety levels as given in Table 1-2 are achieved. It is an implicit requirement that the third party verifier has prior experience and expertise in application of enhanced risk based safety factors.

**Guidance note:**

The documentation required for verification and approval is stated below.

- Safety Factor Assessment Report, which includes the following as a minimum:
  - base case fatigue damage computation
  - stochastic variables
  - probabilistic distributions of variables
  - response surfaces
  - estimated safety factors

In addition, the design basis, the fatigue analysis procedure and the fatigue analysis reports need to be furnished for providing the background information.

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**6.4 Safety factors based on case specific structural reliability analysis**

Reliability, or structural safety, is defined as the probability that failure will not occur or that a specified criterion will not be exceeded.

This section gives requirements for structural reliability analysis undertaken in order to document safety factors in compliance with the offshore standards. Acceptable procedures for reliability analyses are documented in the Classification Note No. 30.6.

Reliability analyses should be based on level 3 reliability methods. These methods utilise probability of failure as a measure and require knowledge of the distribution of all basic variables.

In this Recommended Practice, level 3 reliability methods are mainly considered applicable to:

- special case design problems,
- novel designs where limited (or no) experience exists.

Target reliabilities should be commensurate with the consequence of failure. The method of establishing such target reliabilities, and the values of the target reliabilities themselves should be approved in each separate case.

In compliance with the DNV-OS-F201, a probabilistic design approach based on a recognised structural reliability analysis may be applied provided that:

- the method complies with DNV Classification Note no. 30.6, or ISO 2394, or internationally recognised codes and standards
- the approach is demonstrated to provide adequate safety for familiar cases, as indicated by this standard.

As far as possible, target reliability levels should be calibrated against identical or similar riser designs that are known to have adequate safety based on the DNV-OS-F201. If this is not feasible, the target safety level should be based on the failure type and class as given in Table 1-2.

Suitably competent and qualified personnel should perform the structural reliability analysis, and extension into new areas of

application should be supported by technical verification.

Reliability analysis may be updated by utilisation of new information. Where such updating indicates that the assumptions upon which the original analysis was based are not valid, and the impact of such non-validation on safety, operation and functional consequences need to be re-evaluated.

Additional information and requirements for structural reliability analysis can be found in the following references:

- DNV-OS-F201, “Dynamic Risers”, Section 2, C500.
- DNV Classification Note No. 30.6, "Structural Reliability Analysis of Marine Structures".

**6.5 VIV safety factors**

See Section 4.1, where the VIV analysis methods are summarised. The implicit uncertainty and bias associated with the VIV analysis model, needs to be considered along with the VIV fatigue safety factor, in order to establish the adequate acceptance criterion for VIV induced fatigue.

The acceptance criteria given in Section 4.2 can be generalised as:

$$D_{VIV}(T_{design}) \leq \frac{\alpha}{\gamma} \tag{6.9}$$

where,

- $\alpha$  = Bias factor
- $\gamma$  = VIV fatigue safety factor
- $T_{design}$  = Design life time in years

The bias factor  $\alpha$  accounts for the bias, i.e. expected systematic deviation from the ‘true’ fatigue damage obtained by the applied fatigue analysis methodology.

$$\alpha = \frac{D_{VIV}(applied\ method)}{D_{VIV}(full\ scale\ VIV\ measurements)} \tag{6.10}$$

**Guidance note:**

For example, the “semi-empirical” methods can be the ‘applied method’, and the ‘full scale measurements’ can be used to establish the extent of bias. See Sworn et al. (2003) and Halse (2000), for further guidance on evaluation of the bias.

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The bias within the VIV analysis model is of paramount importance and should be evaluated for all cases. If a conservative VIV analysis model is applied, and if the extent of conservatism is known or documented, then the effect of the bias may be accounted for or conservatively ignored.

**Guidance note:**

If the simplified VIV method based on Section 4.3 is applied, then the bias can be taken as 1, for TTRs.

In all other cases, the bias needs to be quantified and documented.

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The VIV fatigue safety factor,  $\gamma$ , can be established using the risk based fatigue criterion described in Section 6.3. As discussed in 6.3.5, different stochastic variables may be relevant based on the floater type, riser type, environmental modelling, analysis methodology etc. In addition to the stochastic variables, listed in Table 6-4, the uncertainty in the following (not exhaustive) list of variables should also be considered:

- Strouhal number
- bandwidth parameters
- damping
- lift coefficient

- response models (Amplitude /Diameter ratio)
- etc.

**Guidance note:**

The stochastic variables governing the VIV and their associated uncertainties can vary widely from case to case and this in turn will influence the VIV safety factor.

For a limited number of case studies performed by DNV, the VIV safety factor ranged from 10 to 15, for safety class HIGH. This range of safety factors is only indicative and hence it is strongly recommended that case specific VIV safety factors are established based on Section 6.3.

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## 7. Reassessments and Fatigue Life Extensions

### 7.1 In-service fatigue inspections

The S-N curve approach may be used for screening purposes to identify the most likely regions where fatigue cracks may appear during service. Time to first in-service inspection may be based on crack growth versus time results with the criteria given in Table 6-1 in combination with fabrication/installation records. The in-service inspection plans after first inspection should be based on the inspection results obtained and the plans updated accordingly. For defects found, fatigue crack calculations to establish residual life should be based on the sizing accuracy of the applied method and the expected value should be used for fatigue assessment.

Necessary data should be logged during the life cycle for documentation and analysis of fatigue status for temporary risers. The log should typically include running sequence of joints, riser configuration, field data (water depth, pressure, density, etc.), floater data including top tension and the length of time and sea-state for each mode of operation. This log should be reviewed regularly to assess the need for fatigue crack inspections.

In-place Non-Destructive Testing (NDT) or removal of the riser for dry inspection is considered acceptable means of inspection.

It is recommended to use Eddy Current or a qualified NDT approach for inspection of surface cracks starting at hot spots of the risers. The Eddy Current is not so reliant on orientation of defect but can inspect over thin layers of coating.

If the riser has an aluminium spray coating, it needs to be removed before the NDT. Inspections by Dye Penetrants (DP) is not recommended in such cases as any surface breaking defects will be closed over due to the machining/sand blasting. Similar difficulties can be encountered with Magnetic Particle Inspection (MPI). Even though MPI can find subsurface defects, the probability of detection will be reduced dramatically.

Conventional ultrasonics and radiography are not suitable either, as they are volumetric inspection methods, and very reliant on direction and orientation of 2 dimensional defects, i.e. cracks.

The acceptance criteria for the NDT should be at least as stringent as the original acceptance criteria at the time of the fabrication. It is recommended that the acceptance criteria are re-established, taking into account the updated information from the prior service life of the riser. The important issues that need to be considered are discussed in Section 7.3.

It is recommended to identify the most critical weld with respect to fatigue and to do a comprehensive inspection of this weld. In situations where a fatigue life extension is planned and if cracks are not detected, it is still recommended to perform a light grinding at the hot spot area to remove undercuts

and increase the reliability of the inspection.

Detected cracks should be repaired (/ground), cleaned and inspected again to document that they are removed. The remaining life of such a repair should be assessed for each specific case.

For welded connections that are ground and inspected for fatigue cracks the following procedure may be used for calculation of an elongated fatigue life. Provided that grinding below the surface to a depth of approximately 1.0 mm is performed and that fatigue cracks are not found by a detailed NDT of the considered hot spot region at the weld toe, the fatigue damage at this hot spot may be considered to start again at zero. If a fatigue crack is found, a further grinding should be performed to remove any indication of this crack. If more than 10% of the thickness is removed by grinding, the effect of this on increased stress should be included when a new fatigue life is assessed. In some cases as much as 30% of the plate thickness may be removed by grinding before a weld repair is resorted to. This depends on type of joint, loading condition and accessibility for a repair.

It should be noted that fatigue cracks growing from the weld root of fillet welds can hardly be detected by NDT. Also, the fatigue life of such regions can not be improved by grinding of the surface.

It should also be remembered that if renewal of one hot spot area is performed by local grinding, there are likely other areas close to the considered hot spot region that are not ground and that also experience a significant dynamic loading. The fatigue damage at this region is the same as earlier. However, also this fatigue damage may be reassessed taking into account:

- the correlation with a ground neighbour hot spot region that has not cracked
- an updated reliability taking the reliability of performed in-service inspections into account as discussed above.

### 7.2 Extended fatigue life

The following notations are used in this section:

$T_{Design}$	Design service life time in years
$T_{Calculated}$	Fatigue life time calculated in years, without the DFF
$T_{Extended}$	Extended service life time in years
$T_{Prior}$	Prior service life time in years
$T_{Residual}$	Residual service life time in years
$D_{Prior}$	Computed fatigue damage per year during the Prior service lifetime
$D_{Residual}$	Computed fatigue damage per year for the Residual service lifetime

The following relationship exists:

$$T_{Extended} = T_{Prior} + T_{Residual} \quad (7.1)$$

$$T_{Extended} > T_{Design} \quad (7.2)$$

Life time extensions can be treated within the 'normal' design criteria, if the calculated fatigue life during the design phase, is longer than the extended design life times the Design Fatigue Factor.

$$T_{Calculated} > DFF \cdot T_{Extended} \quad (7.3)$$

By 'normal' design criteria, it is meant that the design criteria that were used at the initial design phase of the riser. This is subject to the fact that:

- the riser’s operational conditions (pressure, temperature, density of the riser content; floater motions) during its prior service life were within the design limits set during the design stage
- the actual environmental conditions were at least identical or less conservative than the design environmental conditions during its prior service life
- no cracks are detected during the in-service inspection or no cracks exceed the acceptance criteria
- marine growth and corrosion are within the design limits.

In all other cases a re-evaluation of the residual fatigue life should be performed.

Techniques for improvement of fatigue life by fabrication, is presented in Section 4 of DNV-RP-C203, and can also be used as a means to achieve extended fatigue life.

### 7.3 Reassessment of residual fatigue life

#### 7.3.1 Reassessment scenarios

The reassessment of residual fatigue life can be foreseen in the following scenarios:

- significant deviations of the operational and environmental conditions during in-service lifetime when compared to the design basis and original design criteria
- defects observed at prior inspections
- design changes during the service life of a riser (e.g., top tension reduction for a TTR, floater modifications which in turn will influence the floater motion characteristics, etc)
- extension of service life.

#### 7.3.2 Reassessment requirements

The reassessment of the residual fatigue life should take into account at least the following elements during the reanalysis:

- minor design deviations/changes which were approved in the fabrication phase and which were not included in the original fatigue analysis during the design phase
- the structural modelling of the riser should be based on as-built drawings and as per the fabrication data given in the DFI resume’
- any modifications made to the floater, which can possibly influence the floater motion characteristics
- wear and corrosion
- updated operational data such as riser content density, pressure and temperature, during the service life of the riser
- latest/updated environmental data if available
- recent S-N curves which reflect the state-of-the-art understanding, such as the 2-slope S-N curves given in DNV-RP-C203, can be applied.
- case specific safety factors based on Section 6.5 can also be applied
- if corrosion is not observed, a less stringent S-N curve could be used. However, this relaxation may only be performed based on case specific information.

#### Guidance note:

Consider an example, where the original design basis specifies the applicable S-N curve as “F1 - Seawater with Cathodic protection”. If the inspection reveals that there are no cracks and no corrosion reduction during the prior service life, then a less stringent S-N curve such as “F1 - Air” can be applied for the prior service life, in the fatigue life reassessment. However, for the residual service life, the S-N curve “F1 - Seawater with Cathodic protection” should be applied.

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- results from performed inspections throughout the prior service life, taking into account the reliability of inspection method used and periodicity of the inspection.

- crack growth evaluations considering the crack growth characteristics; i.e. crack length/depth as function of time/number of cycles, stress range, loading type and the possibility for redistribution of stress.

#### 7.3.3 Acceptance criterion

The fatigue life reassessment can be split into 2 phases, namely:

- 1) Prior Service Phase: the fatigue damage that has accumulated since installation until now.
- 2) Residual Service Phase: the fatigue damage that will accumulate from now on.

These computations are to be made in line with the Section 7.3.2.

#### Guidance note:

An alternative approach would be to back calculate accumulated fatigue damage for the ‘Prior Service Phase’, based on the previously computed fatigue life, from the original fatigue design reports and combine it with the new computations for fatigue damage for the ‘Residual Service Phase’. However, such an approach is considered less accurate and inconsistent and hence should not be adopted.

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The acceptance criterion for reassessed riser fatigue can be written as:

$$(D_{\text{Prior}} \cdot T_{\text{Prior}} + D_{\text{Residual}} \cdot T_{\text{Residual}}) \cdot DFF \leq 1.0 \quad (7.4)$$

## 8. References

### 8.1 Codes and standards

- API RP 2 RD “Design of Risers for Floating Production Systems (FPSs) and Tension Leg Platforms (TLP’s)”
- DNV-CN-30.6 “Structural Reliability Analysis of Marine Structures”
- DNV-OS-F101 “Submarine Pipeline Systems”
- DNV-OS-F201 “Dynamic Risers”
- DNV-RP-C203 “Fatigue Strength Analysis of Offshore Steel Structures”
- DNV-RP-C205 “Environmental Conditions and Environmental Loads”
- DNV-RP-F105 “Free Spanning Pipelines.”
- ISO 2394: General principles on reliability for structures, 1998
- UK DOE: Offshore Installations: Guidance on design and construction, April 1984.
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Stahl, B. and Banon, H.: "Fatigue Safety Factors for Deepwater Risers", OMAE 2002-28405, Oslo, Norway, June 2002.
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## APPENDIX A

### A.1 Narrow band fatigue damage assessment

#### A.1.1 General

The basic assumption in narrow-band fatigue damage estimation is that the stress cycles (S) can be determined directly from the stress maxima ( $S_a$ ). Each cycle's range is assumed to be twice the value of the corresponding value of the local stress maximum, yielding:

$$S = 2 \cdot S_a \quad (\text{A.1})$$

Furthermore, the number of stress cycles per unit time is given directly by the zero crossing frequency,  $f_0$  of the stress response process.

#### A.1.2 Narrow band gaussian fatigue damage

If the stress response process is assumed to be narrow banded and Gaussian, the distribution of local stress maxima,  $S_a$ , is defined by a Rayleigh probability density as:

$$f_S(s_a) = \left( \frac{s_a}{\sigma} \right) \exp\left( -\frac{s_a^2}{2\sigma^2} \right) \quad (\text{A.2})$$

where  $s_a$  is the local stress maximum and  $\sigma$  is the standard deviation of the stress response process.

For a linear S-N curve (in log-log scale) the fatigue damage per unit time can be expressed as:

$$D = \frac{f_0}{a} (2\sqrt{2}\sigma)^m \Gamma\left(\frac{m}{2} + 1\right) \quad (\text{A.3})$$

where  $\Gamma(\cdot)$  is the gamma function given by

$$\Gamma(\varphi) = \int_0^{\infty} e^{-t} t^{\varphi-1} dt \quad (\text{A.4})$$

For a bi-linear S-N curve (in log-log scale) the corresponding fatigue damage becomes

$$D = \frac{f_0 \cdot (2\sqrt{2}\sigma)^{m_1}}{a_1} G_1\left\{ \left(1 + \frac{m_1}{2}\right); \left(\frac{S_{sw}}{2\sqrt{2}\sigma}\right)^2 \right\} + \frac{f_0 \cdot (2\sqrt{2}\sigma)^{m_2}}{a_2} G_2\left\{ \left(1 + \frac{m_2}{2}\right); \left(\frac{S_{sw}}{2\sqrt{2}\sigma}\right)^2 \right\} \quad (\text{A.5})$$

where  $G_1$  and  $G_2$  is the complementary incomplete Gamma function and incomplete Gamma function, respectively

$$G_1(x, \varphi) = \int_x^{\infty} e^{-t} t^{\varphi-1} dt$$

$$G_2(x, \varphi) = \int_0^x e^{-t} t^{\varphi-1} dt \quad (\text{A.6})$$

The fatigue damage is hence directly expressed by the standard deviation and zero-crossing frequency of the stress response process. This formulation is of special convenience for frequency domain analyses where results from the global analyses are expressed in terms of the auto-spectral density,  $S(\omega)$ , of

the stress response process.

The standard deviation,  $\sigma$  and zero crossing frequency  $f_0$  are hence given as:

$$\sigma = \sqrt{m_0} \quad (\text{A.7})$$

$$f_0 = \sqrt{\frac{m_2}{m_0}} \cdot \frac{1}{2\pi} \quad (\text{A.8})$$

Where  $m_n$  is the  $n^{\text{th}}$  response spectral moment given by

$$m_n = \int_0^{\infty} \omega^n S(\omega) d\omega \quad (\text{A.9})$$

#### A.1.3 Narrow band Non-Gaussian fatigue damage

For time domain analyses, the two-parameter Weibull distribution model is frequently employed as a generalisation of the Rayleigh distribution for the local maxima (i.e., for Non-Gaussian stress-response processes). The Weibull probability density function is given by:

$$f_S(s_a) = \alpha^{-\beta} \beta s_a^{\beta-1} \exp\left( -\left(\frac{s_a}{\alpha}\right)^\beta \right) \quad (\text{A.10})$$

Note that the Rayleigh distribution in (A.5) is obtained for  $\beta = 2$  and  $\alpha = \sqrt{2}\sigma$

The Weibull distribution may be fitted to the short-term (or long-term) distribution of the local maxima. The Weibull distribution parameters ( $\alpha$ : scale,  $\beta$ : shape) are linked to the statistical moments  $\hat{\mu}$ ,  $\hat{\sigma}$  for the local maxima as follows:

$$\hat{\mu} = \alpha \Gamma\left(1 + \frac{1}{\beta}\right)$$

$$\hat{\sigma} = \alpha \sqrt{\Gamma\left(1 + \frac{2}{\beta}\right) - \Gamma\left(1 + \frac{1}{\beta}\right)^2} \quad (\text{A.11})$$

These equations can be used to establish moment estimates of the distribution parameters with basis in sample estimates  $\hat{\mu}$ ,  $\hat{\sigma}$  from time domain simulations.

The fatigue damage per unit time in the general case of a bi-linear S-N curve can then be expressed analytically as follows:

$$D = \frac{f_0 \cdot (2\alpha)^{m_1}}{a_1} G_1\left\{ \left(1 + \frac{m_1}{\beta}\right); \left(\frac{S_{sw}}{2\alpha}\right)^\beta \right\} + \frac{f_0 \cdot (2\alpha)^{m_2}}{a_2} G_2\left\{ \left(1 + \frac{m_2}{\beta}\right); \left(\frac{S_{sw}}{2\alpha}\right)^\beta \right\} \quad (\text{A.12})$$

### A.2 Wide band fatigue damage assessment

#### A.2.1 General

For marine risers, the stress response is normally neither narrow-banded nor completely wide-banded. In a wide-band response a strict relationship between the stress cycles and stress maxima and minima does not exist. For this reason the distribution of stress cycles can not be evaluated accurately

from the distribution of stress maxima. The following procedures exist to describe fatigue damage for a wide band process:

- cycle counting algorithms,
- semi-empirical solutions, or
- simplified analytical solutions

Wide band fatigue assessment is of special importance for fatigue assessment of combined WF/LF stress response. It is in general applicable to results from time domain analyses but can also be applied in connection with frequency domain analyses through a transformation of frequency domain results to time domain (by e.g. FFT-simulation).

### A.2.2 Cycle counting

The fatigue damage may be obtained by counting the stress cycles in the actual or simulated stress time histories. Specials purpose counting algorithms have been developed with techniques applicable to non-Gaussian stress time histories. The recommend method is the Rain Flow Counting (RFC) method.

The RFC method provides an estimate of the stress probability density function (i.e. sample estimate of  $f_S(s)$ ) and of the average number of stress cycles per unit time). For a linear S-N curve, (2.8) can subsequently be applied for estimation of fatigue damage in each stationary short-term condition. Extension to more general S-N curves (e.g. bilinear) is straightforward.

The response process due to combined wave- and low frequency excitation is generally wide-banded. Time domain simulation and cycle-counting procedures will accordingly be relevant.

Cycle counting methods represent time domain estimates of fatigue damage. Statistical uncertainties will therefore always be present in the estimates. Sensitivity studies should therefore be conducted to document that adequate fatigue damage estimates have been obtained. This is of special importance for combined WF/LF stress time histories or in cases with S-N curves with large (inverse) slope (i.e. large 'm'). Due to the long periods of the LF floater motion, longer simulations are typically required in order to reduce the statistical uncertainties for combined LF/WF motions than for pure WF response.

### A.2.3 Semi-empirical solutions

A number of semi-empirical expressions have been proposed in the literature to correct the narrow band fatigue damage calculation for the effects of a broad bandwidth. An often used approach is based on the assumption that the true damage  $D_{RFC}$  (i.e. using a rain flow counting technique) can be established from a corrected narrow-band result:

$$D_{RFC} = D_{NB} \kappa_{RFC} \quad (A.13)$$

where  $D_{NB}$  is the narrow banded Gaussian fatigue damage given in A.1.2 and  $\kappa_{RFC}$  is a correction factor. Wirshing and Light (1980), see e.g. Barltrop & Adams (1991) proposed the following expression:

$$\begin{aligned} \kappa_{RFC}(m) &= a + (1-a)(1-\varepsilon)^b \\ \text{where} & \\ a &= 0.926 - 0.033m \\ b &= 1.587m - 2.323 \end{aligned} \quad (A.14)$$

where  $\varepsilon$  is the bandwidth parameter defined by (note that  $\varepsilon = 1$  for a broad banded process and  $\varepsilon = 0$  for a narrow banded process):

$$\varepsilon = \sqrt{1 - \frac{m_2^2}{m_0 m_4}} \quad (A.15)$$

As a promising alternative, Dirlik, see e.g. Barltrop & Adams (1991) proposed an empirical closed form expression for the stress probability density function.

The fatigue damage expression given by Lutes and Larsen (1990, 91) involves one moment of the spectral density function and can be written as follows:

$$\bar{D} = \frac{T}{2\pi a} \cdot (2\sqrt{2})^m \cdot \Gamma\left(\frac{m}{2} + 1\right) \cdot (\lambda_{2/m})^{m/2} \quad (A.16)$$

Where  $\bar{D}$  is the fatigue damage, and the number of cycles to failure is given as:

$$N = \bar{a} S^{-m} \quad (A.17)$$

The single moment is written as:

$$\lambda_{2/m} = \int_0^{\infty} \omega^{2/m} \cdot G(\omega) \cdot d\omega \quad (A.18)$$

where:

- $\bar{D}$  = fatigue damage
- T = duration
- N = number of cycles to failure
- $\bar{a}$  = fatigue constant from the S-N curve
- m = fatigue exponent from the S-N curve
- S = stress range
- $\omega$  = frequency
- $G(\omega)$  = spectral density function of the stress range

### A.2.4 Analytical solutions for bi-modal spectra

Accurate analytical solutions to fatigue damage estimates can be obtained for well-separated bi-modal stress spectra (e.g. a process with a combination of low frequency and wave frequency Gaussian component). See Jiao & Moan (1990), where a correction function on a form similar to (A.13) have been derived by analytical means assuming two independent narrow-banded Gaussian process. See DNV-OS-E301, "Position Mooring", Chapter 2, Section 2 for details.

In case the process may be assumed to be composed of two independent Gaussian stress response processes an upper bound on the estimated fatigue damage can be obtained by adding the variances of the contributions directly. The zero-crossing frequency may be expressed as a combination of the respective zero-crossing frequencies based on expressions for the sum of two independent Gaussian processes.

## A.3 Combining fatigue damage by the simplified DNV method

### A.3.1 General

In many design cases, the contributions to fatigue damage can be due to different constituent dynamic processes. A typical example is the fatigue damage from wave frequency damage and low frequency damage. In earlier phases of the design the fatigue damage at a hot spot may be calculated separately from the constituent dynamic processes. In a few cases it may even be practical to calculate the fatigue damage for each of these processes separately. A typical example is the VIV fatigue damage, which is usually calculated independent of the WF/LF fatigue damage.

It may be non-conservative to simply add the two fatigue damages together as stated earlier in Section 5. The present section provides a simple but conservative methodology for combin-

ing the resulting fatigue damage from two different processes. When a detailed stochastic analysis is performed for each of the dynamic processes, a combined stress response can be calculated, before the S-N curve is entered and the fatigue damage is calculated. The methodology is based on information of mean zero up-crossing frequency in addition to the calculated fatigue damages for each of the processes.

### A.3.2 Resulting fatigue damage for a single slope S-N curve

A single slope S-N curve in a logarithmic format is assumed for the following derivation of resulting fatigue damage.

$$\text{Log}N = \log(\bar{a}) - m \log(S)$$

or

$$N = \bar{a} S^{-m}$$

where

- $\bar{a}$  log = intercept with the log N axis
- m = negative inverse slope of the S-N curve
- S = stress range

The fatigue damage is calculated based on Palmgren-Miner rule as:

$$D_i = \sum \frac{n_i}{N_i}$$

Number of cycles is linked to the mean zero up-crossing frequency,  $v_i$ , of the response as:

$$n_i = v_i T_d$$

where

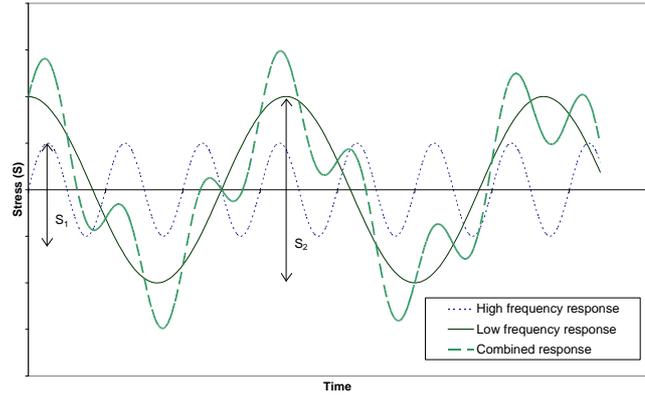
$T_d$  = Design life in seconds.

From equations (A.19) to (A.21) the fatigue damage is calculated as:

$$D_i = \frac{v_i T_d (S_i)^m}{\bar{a}}$$

From this an equivalent stress range can be obtained as:

$$S_i = \left( \frac{D_i \bar{a}}{v_i T_d} \right)^{1/m}$$



**Figure A-1**  
Sketch showing high and low frequency response and combined response

See Figure A-1, where a schematic sketch shows a high frequency response, a low frequency response and the combined response. An analogy with rain flow counting is considered. It is seen that the fatigue damage using a stress cycle in process no 2 alone is too low.

The fatigue damage for this process is increased by assuming that the high frequency response can be added to that of the low frequency response to get a cycle with a stress range of  $S_1 + S_2$ . Then the fatigue damage for the low frequency response can be increased as shown in the second term of the right hand side of the equation (A.24) below.

Note that  $n_2$  cycles of the stress range  $S_1$  becomes included in the resulting fatigue calculation of  $D_2$ . Hence it is subtracted from  $D_1$  in order not to include it twice.

Then the resulting fatigue damage can be calculated as:

$$D = D_1 + D_2 \left( \frac{S_1 + S_2}{S_2} \right)^m - D_1 \frac{n_2}{n_1}$$

where

- $D_1$  = calculated fatigue damage for the high frequency response
- $D_2$  = calculated fatigue damage for the low frequency response
- $S_1$  = representative stress range for the high frequency response
- $S_2$  = representative stress range for the low frequency response
- $n_1$  = number of cycles during design life from the high frequency response
- $n_2$  = number of cycles during design life from the low frequency response

Then the equivalent stress from (A.23) is used as representative stress in equation (A.24) and by introducing (A.21) the following equation for resulting combined fatigue damage is obtained

$$D = D_1 \left( 1 - \frac{v_2}{v_1} \right) + v_2 \left\{ \left( \frac{D_1}{v_1} \right)^{1/m} + \left( \frac{D_2}{v_2} \right)^{1/m} \right\}^m$$

where

- D = combined fatigue damage from the high frequency and low frequency response
- $\nu_1$  = mean zero up crossing frequency for the high frequency response
- $\nu_2$  = mean zero up crossing frequency for the low frequency response.

### A.3.3 Fatigue damage for two-slope S-N curves

The presented approach and equation (A.25) can also be applied for two-sloped S-N curves for calculation of resulting combined fatigue damage.

#### Guidance note:

The “S-N curves for air” given in DNV-RP-C203, have a transition in slope from  $m = 3.0$  to  $m = 5.0$  at  $10^7$  cycles.

For a long term stress range distribution with Weibull shape parameter of 1.0, designed for 20 years service life, and a fatigue damage equal 1.0 the major contribution to fatigue damage occurs around  $10^7$  cycles. Approximately half the damage occurs at number of cycles below  $10^7$  cycles and the other half above  $10^7$  cycles. For lower fatigue damage than 1.0, which is the case in order to have acceptable resulting fatigue damage when considering two processes, the main contribution to fatigue damage will be at the S-N line with slope  $m = 5.0$ .

Thus, in order to have a methodology that is safe one should use a slope  $m = 5.0$  in equation (A.25) if the fatigue damages for the two processes have been calculated based on this two-slope S-N curve.

An alternative to this is to calculate the fatigue damage for process no 2 with a straight S-N curve with slope  $m = 3.0$ . Then equation (A.25) can be used with  $D_1$  calculated from a two-slope S-N curve and with  $m = 3.0$ .

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## A.4 Aspects of fatigue performance

### A.4.1 Examples of fatigue critical areas

Examples of critical areas regarding fatigue damage of metallic risers are given in the following:

- the areas close to upper/lower termination of top tensioned risers will normally experience significant dynamic bending stress variation. Fatigue close to upper termination is normally governed by WF stress cycles while LF response may be of significance close to the seafloor termination. Accurate modelling of boundary conditions and stiffness properties is required (e.g. taper joints, stiffness characteristics of flex-joints etc)
- the splash zone is normally a critical area for top tensioned as well as compliant riser configurations mainly due to WF bending stress cycles. Description of wave loading up to actual wave elevation is of vital importance for accurate prediction of fatigue damage Due regard should also be given to possible disturbances in the wave kinematics caused by the presence of the floater. Time domain analyses supported by sensitivity studies to confirm adequacy of load model is recommended (i.e. results are sensitive to mesh size as well as wave kinematics)
- seafloor touchdown area is a critical area for steel catenary risers and other proposed compliant riser configurations. Soil properties, mesh size and mean floater position are important for prediction of fatigue damage. Time domain analyses are generally recommended together with sensitivity studies to support rational conservative assumptions regarding soil properties. The adequacy of the mesh applied in the touchdown area should also be confirmed by sensitivity studies, and
- considerations regarding resonance dynamics and combined WF and LF fatigue damage are of special impor-

tance for spar risers (in particular for integral air-can solutions). Critical locations are typically close to riser supports in the hull area. Special attention should be given to possible LF stress cycles at the keel joint.

### A.4.2 Improving the predicted fatigue life

It should be noted that there is no “off the shelf” solution for improving the predicted fatigue life of a riser. The following section is intended as a general guidance and is not intended to replace the required parametric studies and design optimisation studies.

In principle, fatigue life can be improved by design changes, fabrication process improvements and in some case by refined analysis methods. The fatigue life improvements based on fabrication improvements is also discussed in Section 4 of DNV-RP-C203, for the offshore steel structures.

#### A.4.2.1 Design

- Modified thicker sections in the weld zone, which are to be welded on the barge. In Figure A-2, a modified end section at the weld joint is shown. The welds at location ‘a’ are performed at shore. Hence superior quality welds can be achieved for the weld performed onshore by means of high quality welding, which can be followed by grinding/machining. Further defect size can be controlled onshore for these welds, using suitable NDT methods. This implies a much better S-N curve and lower SCF will be applicable for the location ‘a’ shown in Figure A-2. The thicker sections of two adjoining riser section can be welded offshore (i.e. onboard a weld barge) at location ‘b’, as shown in Figure A-2. By taking advantage of the thicker section, associated SCF, the fatigue life can be improved at location ‘b’.
- Increased wall thickness (typically at the TDZ for the SCRs) may provide better fatigue life.
- Buoyancy modules in TDZ can be introduced and its influence on the fatigue life can be assessed.
- The sensitivity of the hang-off angle can be studied and the hang-off position and angle can be optimised.
- Modifications in top tension for TTRs can be attempted within the design range to study and improve the fatigue life. The VIV induced fatigue will be sensitive to the top tension and this can be beneficially used, if the top tension can be optimised.

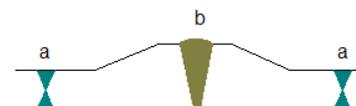


Figure A-2  
Modified thicker end sections at the welded joint locations

#### A.4.2.2 Fabrication

- The fatigue life at the root of the weld (inner surface hot spot) may be governing in a few cases, possibly due to corrosive medium. To overcome this problem, the internal surface of the pipeline can be clad with a suitable corrosion resistant metal. By doing so, an S-N curve corre-

sponding to a better environment can be applied leading to improved fatigue life.

**Guidance note:**

Consider an example SCR case with the following data:

- the S-N F1 curve is applicable at the root of the single sided weld as given by DNV-RP-C203.
- internal fluid in the riser is assumed to be corrosive in nature and hence the “F1 free corrosion” curve is applicable.
- fatigue analysis indicates that the fatigue capacity is marginal or does not meet the acceptance criteria in the TDZ.

In this case, the internal surface of the riser at the TDZ can be clad with a suitable non-corrosive metal. This implies that the “F1 air” curve is applicable, instead of the “F1 free corrosion” curve, which will translate to significantly longer fatigue life.

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- Corrosion protection for the external surface. By including suitable corrosion protection, the medium (environment) of the S-N curve can be improved (for example from ‘free corrosion in seawater’ to ‘cathodic protected in seawater’). If the sacrificial anodes are used for corrosion protection, the consumption of the anodes needs to be monitored / inspected periodically.
- In general grinding has been used as an efficient method for reliable fatigue life improvement after fabrication. Grinding also improves the reliability of inspection after fabrication and during service life. However, experience indicates that it may be a good design practice to exclude this factor at the design stage. The designer is advised to improve the details locally by other means, or to reduce the stress range through design and keep the possibility of fatigue life improvement as a reserve to allow for possible increase in fatigue loading during the design and fabrication process. It should also be noted that if grinding is required to achieve a specified fatigue life, the hot spot stress is rather high. Due to grinding a larger fraction of the fatigue life is spent during the initiation of fatigue cracks, and the crack grows faster after initiation. This implies use of shorter inspection intervals during service life in order to detect the cracks before they become dangerous for the integrity of the connection.

#### A.4.2.3 Refined analysis

- Refined analysis methods give a better estimate of the predicted fatigue life, however, they may not always give “improved” fatigue life.
- The fatigue life can be computed using a time domain approach for the combined Wave Frequency and Low Frequency excitation. Rain-Flow counting can be used to compute the accumulated fatigue damage.
- The floater, risers and mooring lines make up a global system, and in some cases the coupling effect can be dominant. In such cases, the fatigue life computed based on Coupled analysis, will yield more accurate fatigue results.
- Inclusion of the wave spreading effects, will provide more accurate fatigue estimates.
- Refinement of currents (both current velocity binning range refinement and inclusion of the effect of directionality) will provide better VIV fatigue estimates.
- The WF, LF and VIV fatigue damage can be spread at the touch down zone for the Steel Catenary Risers. Taking into account the draft variations and the tidal variations, over the operational life time of a floater, can help in spreading the fatigue damage at the TDZ. Methods for spreading the fatigue damage, to get more realistic estimates, is discussed in A.4.3.
- Risk based safety factors, as described in Section 6.3.

### A.4.3 Spreading the fatigue damage at TDZ

#### A.4.3.1 General

The fatigue damage at the touch down zone (TDZ) of the SCRs can be overestimated, if the variation in the touch down point (TDP) is not taken into account in the riser fatigue analysis. By correctly accounting for the variation of the TDP, during the lifetime of the riser, the fatigue damage can be distributed at the TDZ. Benefits can be seen for both WF and LF fatigue damage, as well as the VIV fatigue damage.

#### A.4.3.2 VIV fatigue damage spreading

Typically the initial VIV fatigue analysis is performed with the floater in the neutral position. The mode shapes are given as input to the semi-empirical VIV software and where the mode shapes are based on the floater in the neutral position. No dynamic forces or motions are accounted for in this initial analysis. Under these circumstances, the predicted fatigue damage in the TDZ may peak sharply at anti-nodes of the calculated mode-shapes, where the curvature and bending stress have peaks. This results in large fluctuations in overall predicted fatigue life between anti-nodes. The extent of this effect will depend on the mode shapes that are activated and the number of modes that are participating.

However in reality, the riser system properties and boundary conditions will not be stationary with time. The TDP can shift locations under the influences of vessel motion, direct hydrodynamic loading on the riser, the riser mass variations etc. This implies that the constant mode-shapes assumptions applied in the VIV analysis is inaccurate. The true life of the riser will be greater than that predicted by the ‘constant riser system’ assumed in the initial VIV analysis. In fact, the locations on the riser of the modal anti-nodes will move around significantly. If the variation of the TDP is properly accounted for, it will smear out peaks and troughs in the calculated damage curve.

The following factors can contribute to the movement of the TDP:

- Wind loads, second order wave loads and current loads on the floater, can modify the floater offset, causing the location of the TDP to change.
- Floater draft changes due to loading (e.g. full loaded FPSO, partially loaded FPSO or under ballast conditions) and tidal variations.
- Variations in the direct current loads on the riser.
- Floater location can be intentionally shifted during the operational lifetime of the field. (e.g. to make way for drilling)
- Long term variation in the riser contents density.
- Riser mass variations due to corrosion or water absorption in buoyancy elements or changes in riser hydrodynamic diameter due to marine growth.
- Riser/Soil interaction – trenching or soil suction in the TDZ, which could influence the TDP.

The relative importance these factors is system dependent and case specific. It is recommended that the analysis should not take into account too many factors to achieve satisfactory fatigue life. It is suggested that sensitivity studies are performed to identify the key factors, which can be practically applied in the analysis.

The fatigue damage can be distributed by the following options:

- The riser can be analysed at different floater offsets and the individual fatigue damage can be computed. The probability of the riser being at these different offsets is applied as a corrective factor on these fatigue damages and summed up to provide the distributed fatigue life at the TDZ.

- The riser can be analysed at the neutral floater position and the damage is spread using statistics. The VIV fatigue damage is spread over a spreading length,  $L_S$ . The contributing characteristic lengths,  $L_{Ci}$  can be determined for the different causes of TDP movement and summed together using equation . Comprehensive statistical treatment of all influences on fatigue damage distribution is possible but may not be normally required.

$$L_S^2 = \sum L_{Ci}^2 \tag{A.26}$$

#### A.4.3.3 WF/LF fatigue damage spreading

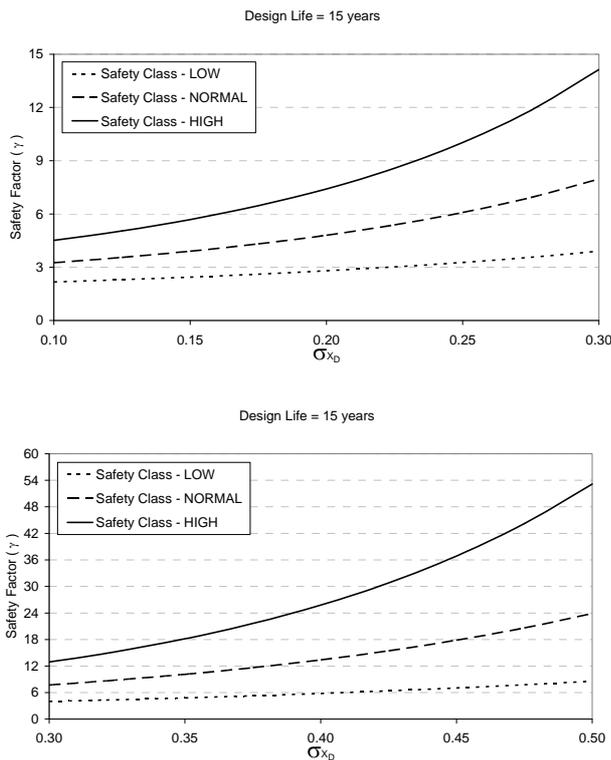
The same principles that are applied for spreading the VIV fatigue damage can also be applied for WF/LF fatigue damage. However, the following point should be adhered:

- consistency in the design approach, i.e. the spreading of the fatigue damage should be done consistently, i.e. with out conflict with other design assumptions / constraints
- spreading should be based on realistic variation of the TDP and should be on the conservative side.

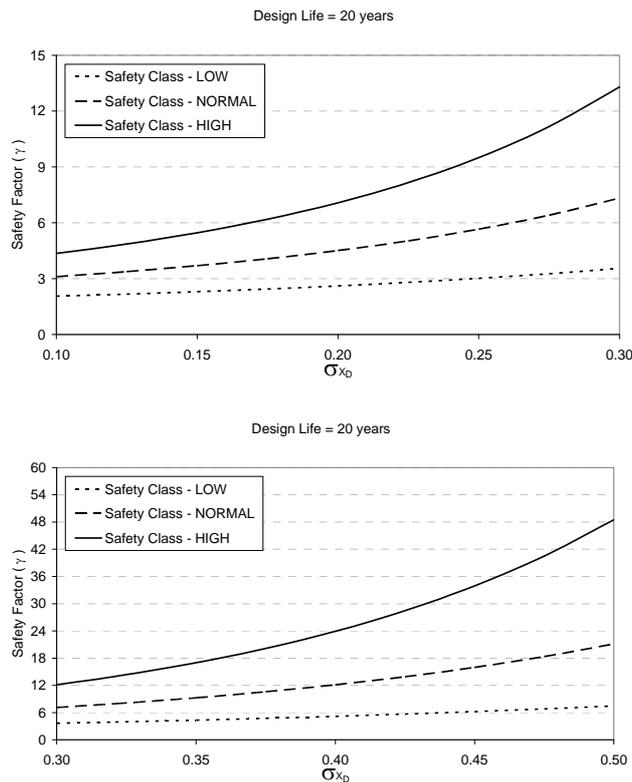
### A.5 Plots of risk based fatigue safety factors

#### A.5.1 General

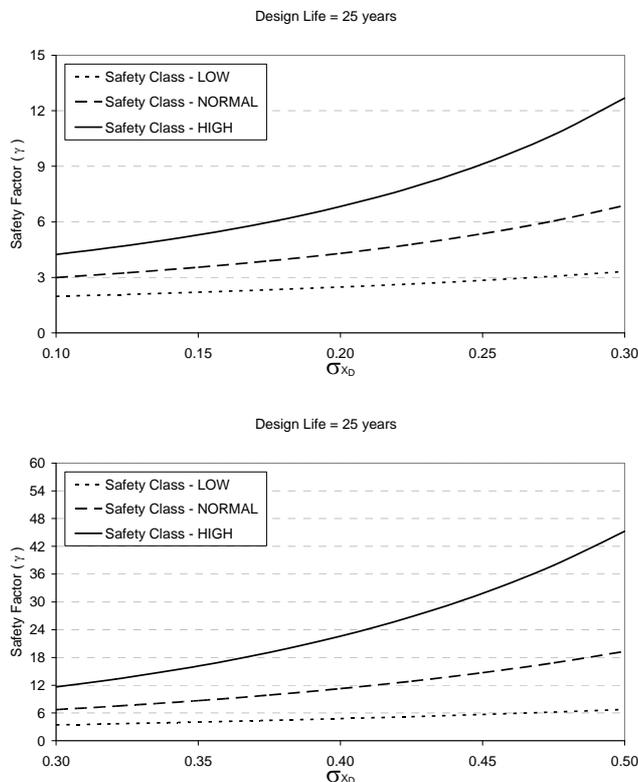
Sample safety factors based on Section 6.3, estimated with 2-slope S-N curves of DNV-RP-C203, with the  $\sigma_{Xa}$  of 0.20., for different design life durations.



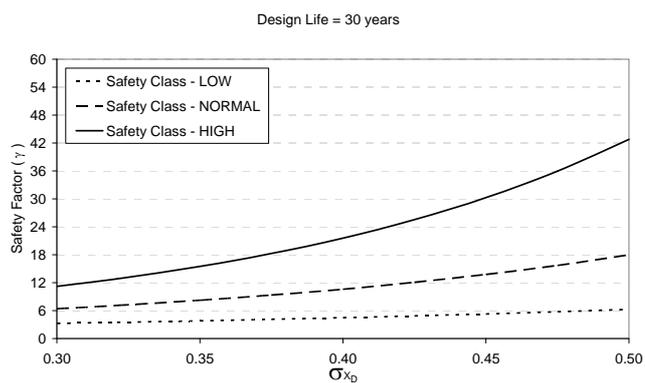
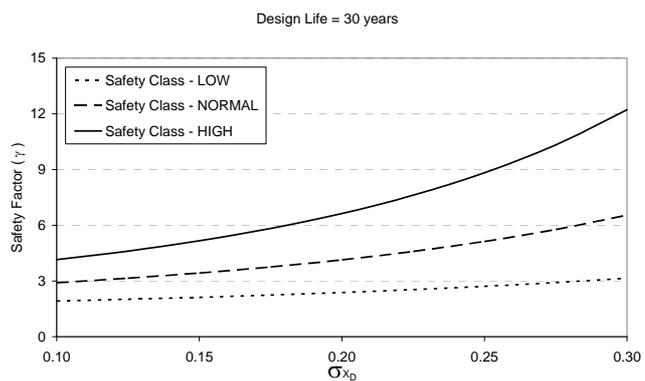
**Figure A-3**  
Safety factors for design life of 15 years



**Figure A-4**  
Safety factors for design life of 20 years



**Figure A-5**  
Safety factors for design life of 25 years



**Figure A-6**  
Safety factors for design life of 30 years