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Target levels for reliability-based assessment of offshore structures during design and operation

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Target levels for reliability-based assessment of offshore structures during design and operation

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SUMMARY

This report deals with approaches for establishing consistent target levels for structural reliability analysis with reference to ultimate and fatigue limit states of components and system; in new as well as existing structures. The target failure probabilities should be referred to annual or service-life values. In principle the target level can be defined to reflect all hazards (e.g. loads) and failure modes as well as the different phases (in-place and temporary phases) for each relevant consequence (fatalities, pollution, loss of assets and the most severe criterion would then govern the decisions to be made. However, it is proposed herein to allocate a certain portion of the allowable total risk (probability of global failure) to each group of hazards and each failure mode (e.g. ultimate or fatigue failure of a component), each phase (e.g. in-place condition depending upon consequence of failure. It is emphasised that the target level should depend upon the reliability methodology applied, especially which uncertainties are accounted for; the cause and mode of failure; the consequences of failure as well as the expense and effort to reduce the risk of failure. It is also important to establish a reliability methodology, which is in accordance with design practice for well-known cases. Target failure probabilities given in general guidelines for structural reliability analysis should not be applied before they are justified for the relevant application.

CONTENTS

1. INTRODUCTION

Adequate structural safety of offshore structures is ensured by design, as well as load or response monitoring, or inspection; and by taking the necessary actions to reduce loads directly or indirectly, by, e.g., removal of marine growth, or to repair, when necessary.

Design criteria relating to safety involve ultimate and fatigue limit state criteria for components. Fatigue is an important consideration for structures in areas with more or less continuous storm loading (such as the North Sea) and especially for dynamically sensitive structures. Progressive limit state criteria for structural integrity were first implemented in codes for the Norwegian Continental Shelf in the 1980's (NPD 1977). The purpose of such criteria is to avoid catastrophic accidents, i.e., system failure due to a small damage, e.g., caused by ship impacts, fires and explosions, and other accidental loads, as well as environmental hazards.

Inspection and repair are important measures for maintaining safety, especially with respect to fatigue, wear and other deterioration phenomena. But their effect on the reliability depends upon the quality of inspection, e.g. in terms of detectability vs. size of the damage. Hence, an inspection and repair measure can contribute to the safety only when there is a certain damage tolerance. This implies that there is an interrelation between design criteria (fatigue life, damage tolerance) and the inspection and repair criteria. Up to now, however, this interrelation has not been explicitly considered, due to lack of methods to deal with this problem in a rational way.

During operation of offshore structures a reassessment of the safety may be necessary due to

- required change of live loads
- occurrence of deterioration or overload damage
- extension of service life, which currently is of concern for many existing platforms
- reduction of operational costs

Such assessments should take into account information about the geometry and material properties acquired during fabrication and observed response or damages during previous inspection, or condition assessments or observed survival of proof-loads or service loads.

Reliability methods are increasingly used to make optimal decisions regarding safety and life cycle costs of offshore structures (see e.g. ISSC, 1988-1994; Moan, 1994). Such methods deal with the uncertainties associated with design, fabrication and operation of structures, and may be classified as follows:

- classical structural reliability analysis (SRA), (see e.g. Melchers, 1987). The purpose of SRA is to determine the failure probability considering fundamental variability, and natural and man-made uncertainties due to lack of knowledge
- quantitative risk analysis (QRA) which deals with estimation of likelihood of fatalities, environmental damage or loss of assets in the broad sense.

Traditionally, failure probabilities of components and systems calculated by SRA are considered notional values. In classical reliability analysis, as e.g. applied in connection with electronic components as well as in QRA, component reliability is usually determined by failure data of real components. The corresponding system properties are commonly determined by systems models. However, SRA is sometimes used to provide input about probabilities of structural failure into quantitative structural risk analysis (QSRA), possibly even QRA (see e.g. Moan, 1993). Care should then be exercised in providing as realistic failure probabilities as possible.

This report deals with structural reliability methodology applied in connection with ultimate and fatigue limit states of platforms under permanent, live and environmental loads. The failure probabilities provided by SRA are then compared to the relevant target levels. The remaining part of this report will be devoted to procedures for establishing target levels.

2. TARGET LEVELS

2.1 GENERAL

The target safety level should depend upon the following factors (see e.g. Eurocode 1, 1993; Moan, 1995):

- method of SRA or QSRA analysis, especially which uncertainties are included
- failure cause and mode
- the possible consequences of failure in terms of risk to life, injury, economic losses and the level of social inconvenience.
- the expense and effort required to reduce the risk of failure.

The safety level is affected by different

- initiating events (hazards) such as environmental loads, various accidental loads, .. which may lead to different
- structural failure modes of components and system which ultimately may cause consequences such as fatalities, environmental damage and loss of assets.

In principle a target level which reflects all hazards, (e.g. loads) and failure modes (collapse, fatigue, ...) as well as the different phases (in-place operation and temporary phases associated with fabrication, installation and repair) could be defined with respect to each of the three ultimate consequences and the most severe of them would govern the decisions to be made. If all consequences were measured in economic terms, a single target safety level could be established.

However, in practice it is convenient to treat different hazards, failure modes, and phases separately. A certain portion of the total (target) failure probability may then be allocated to each case, assuming e.g. that the total failure probability is just equal to the sum of the individual probabilities. Often the simplification is made to treat the different hazards, failure modes and phases separately. The present report deals with target levels for offshore structures subjected to permanent, live and environmental loads. This may be reasonable because rarely do all hazard scenarios and failure modes contribute equally to the total failure probability for a given structure. In particular the principle of establishing target levels for each hazard separately was adopted by NPD for accidental loads; see e.g. Moan (1993). It was also advocated recently by Cornell (1995).

2.2 RELIABILITY METHODOLOGY

As indicated above SRA and QSRA account for different uncertainties, and SRA may in a sense be considered a special case of QSRA. In particular, QSRA accounts for human factors, which may lead, to accidental loads and abnormal resistance. Even in SRA the various uncertainties may be assessed in different ways as discussed in Chapter 4. Clearly, the corresponding target safety levels will differ.

It is emphasised in this report that consistent and state-of-the art reliability methods should be applied in establishing target levels. However, simplified methods which provide a closed-form expression for failure probabilities, but which do not necessarily represent the state-of-the art approaches, are used in this report to illustrate basic principles and procedures that are proposed.

2.3 FAILURE CAUSE AND MODE

The most important distinction of different failure causes is due to whether they are instantaneous or progressive - i.e. takes time. Sometimes the notions: failure preceded and not preceded by a warning are used. The most relevant practical examples would be an instantaneous overload failure versus a gradually developing fatigue failure or other deterioration, respectively. The failure development over time may influence the failure consequences since a warning may initiate escape and evacuation of personnel.

The most important distinction of structural failure modes is between component and system modes. This fact also has a bearing on consequences of failure and is further discussed in Chapter 2.4.

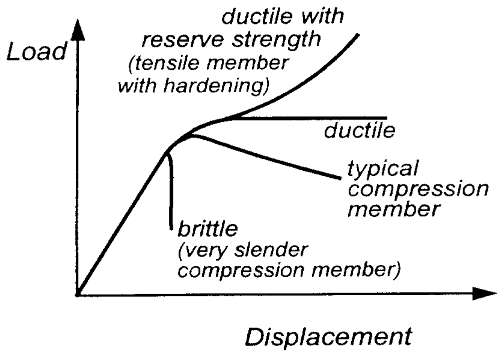
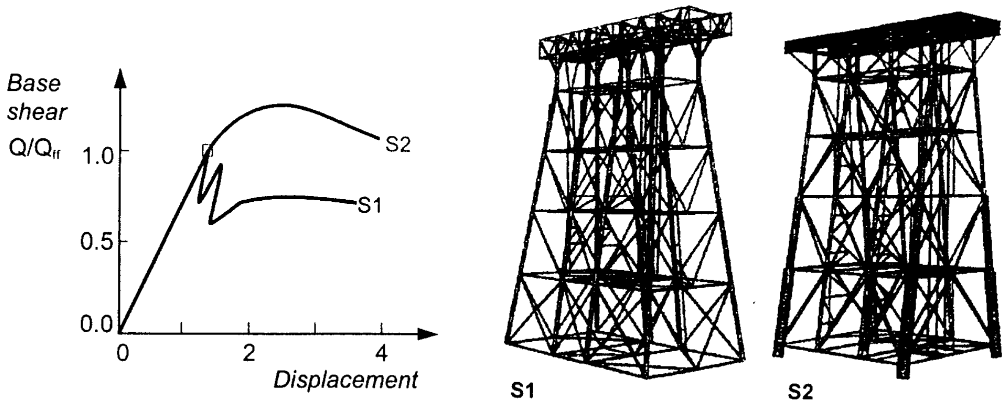


Figure 1
Load-deformation characteristics of components.

It is observed that the component characteristic: reserve strength vs. no reserve strength (see Fig. 1 affects the behaviour of a statically determinant system under static loading. Whether the component behaviour is ideally elasto-plastic (ductile) or brittle does not have any influence on such systems. For a redundant structure, however, the system strength will depend upon the component characteristic (Fig. 1), as well as the system composition of components. Fig. 2 shows typical behaviour of jackets under broadside static loading. The statically determinate K-braced jacket fails in a "brittle manner" while the failure mode of the X-braced structure is more “ductile”, as well as yield a reserve strength beyond first member failure.

The implication of the mentioned characteristics of structural behaviour in connection with target safety levels will depend upon whether target reliability measures for components or systems are required. In case of ultimate strength design of components the question of residual strength (or, more precisely conditional failure probability) overrides the effect of the component characteristics illustrated in Fig. 1.

Ultimate systems strength may be discussed with reference to Fig. 2. Obviously, both the ultimate strength and post-ultimate behaviour for the Cases S1 and S2 differ. The more ductile post-ultimate behaviour of Case S2, would have no impact on the target level if the loading is truly static (force). However, the ultimate behaviour under dynamic (wave) loading may differ significantly, depending upon the post-ultimate behaviour. This is because the external loads then will be (partly) balanced by inertia forces. Thus, a jacket with ductile overall behaviour may exhibit an ultimate strength which exceeds the static capacity by, say, 10-20%, see e.g. Bea and Young (1993) and Azadi et al (1995). If the reliability analysis is based on the ultimate strength limit determined by static (pushover) analysis a differentiation in the target level could be made depending upon the mentioned post-ultimate behaviour.



a) Load displacement behaviour.
Qff is base shear at first member failure.

b) Lay-outs of jackets S1 and S2.

Figure 2
Global behaviour of jacket structures subject to broadside loading (Hellan, 1995).

The ultimate limit of a jacket as a whole is commonly based on load capacity. However, it should be noted that excessive displacement (before ultimate load is reached) may have to be considered as the ultimate limit because of the loads/deformations thereby imposed e.g. on risers.

Fatigue failure may refer to the following limit states:

- occurrence of visible crack
- through-thickness crack of a welded joint of a limited width (a specimen representing a part of a steel-plated structure or a complete tubular member (joint)
- loss of bearing capacity of a member

In standard design procedures based on SN-data, the first two criteria are applied. Clearly, a through-thickness crack may not necessarily be at serious "failure mode".

It is noted that stochastic loading may induce failure associated with steady propagation of crack (fatigue) as well as a possible rupture caused by extreme load effects on a structure with reduced fracture resistance due to crack propagation.

Some structures, notably jackets and mooring systems of floating platforms, consist of slender components, and the important issue is whether the system can survive fatigue failure of one component. Since inspection and repair may be used to ensure safety in connection with deterioration phenomena like fatigue, the design target safety level may be reduced contingent upon the effect of inspection and repair.

2.4 FAILURE CONSEQUENCES

In principle the target safety level is set with respect to consequences in terms of fatalities or human injury, environmental damage and economic losses. However, besides these consequences the platform owner/operator may lose reputation, both from the public as well as from the government. This is a consequence, which is hard to quantify, but it may affect the potential of future licences for hydrocarbon exploitation and hence the business at large, and is, therefore, sometimes considered part of the economic consequences.

To obtain target levels for SRA and QSRA, the mentioned above consequences need to be related to structural failure modes.

Fatalities induced by structural failure of offshore platforms occur primarily when the support of the deck fails or the platform capsizes, i.e. system failure occurs. Failure of individual components (members, joints) commonly does not lead to fatalities. Clearly, the risk of fatalities would depend upon whether platforms will be evacuated before or during the accidental scenario or not. For instance, the likelihood of fatality caused by storm overload in the Gulf of Mexico (GOM) may be less than in the North Sea (NS) because most platforms in GOM can be evacuated in face of a storm (hurricane), while this is not the case in the NS.

Environmental damage may occur due to direct damage to risers/conductors, piping or process equipment; or structural failure. Potential environmental damage depends upon the safety systems (subsea safety valves etc.) available.

Cost-benefit considerations in monetary terms should in principle be applied in connection with setting serviceability limit state criteria. However, the practical implementation may be difficult due to lack of adequate data.

The cost-benefit of increasing the safety would be judged differently depending upon the socio-economic system in the actual geographical region.

2.5 REFERENCE PERIOD FOR TARGET FAILURE PROBABILITY

The target failure probability should be referred to a given time period, i.e. a year or the service life. If the relevant consequence is fatalities, annual failure probabilities are favoured to ensure the same fatality risk of individuals at any time. This principle means that the target level should not depend upon the number of people at risk, i.e. not include risk aversion. This issue is discussed further in Section 3.3. However, in general, as discussed in Chapter 2.4 the target level (with respect to environmental damage and economic loss) should depend upon the potential consequences.

3. METHODS FOR ESTABLISHING TARGET LEVELS

3.1 GENERAL

Various methods may be applied to establish the target level, see e.g. Flint (1976), CIRIA (1977), Faulkner (1983), Jordaan (1988), ISSC (1991), Iwan et al. (1993) and Paté-Cornell (1993). The following approaches will be discussed herein:

a) the implicit safety or risk level implied by existing codes; or in actual structures which are considered acceptable
b) the experienced likelihood of fatalities, environmental damage or property loss associated with operations which are considered acceptable
c) cost-benefit criteria

These methods are briefly described in the following.

3.2 TARGET LEVEL FOR STRUCTURAL RELIABILITY ANALYSIS BASED ON THE SAFETY LEVEL IMPLIED BY EXISTING CODES

In this case the target level is commonly taken to be the implied probability of structural failure in given codes or guidelines which are judged to be acceptable. To achieve a representative target level, several cases of structural geometries, material properties and load conditions should be considered. The implied failure probability will therefore vary and the target level should be based on the mean value or some other measure of the implied failure probability. Obviously, if a relaxation of safety level is desirable, a higher value than the mean is selected. Different levels may be used depending upon the mode of failure, consequences of failure etc. In particular it is necessary to make a distinction between target level for structural components and system. If a single target value for structural design is applied, the target value could be a weighted mean, with a weight factor, which depends upon the consequence of failure for the different components considered.

This method is pursued in more detail in Chapter 4.

3.3 TARGET LEVEL FOR STRUCTURAL RISK ASSESSMENT BASED ON ACCIDENT EXPERIENCES

In structural risk assessment the probability of fatalities, pollution and property loss is estimated, and the target level should in principle be referred to each of these consequences. The focus has been on fatalities and the reference value is the annual death rate in the society, which is of the order $p_d = 10^{-4}$ (Flint 1976; Jordaan and Maes 1991). Paté-Cornell (1993) suggests 10^{-3} to 10^{-4} to be an upper bound of acceptable fatality rate. The target structural annual failure probability based on death rate may be taken to be

$$p_{FT} = \frac{1}{f(n_r)} K_s \cdot 10^{-4} \tag{3.1}$$

where K_s, is a social criterion factor which should be related to the extent to which the activities associated with the structure is hazardous and voluntary (Flint, 1976). $f(n_r)$ is a risk aversion function of the total number of people, n_r, at risk. $f(n_r)$ has e.g. been proposed to be $f(n_r) = n_r$ (Flint 1976) and $f(n_r) = n_r^{1/2}$ (e.g. Allen 1981). Obviously, if the concern is individual fatality rate, $f(n_r)$ should be put equal to 1.0.

A more detailed picture of the accident rate in offshore operations is displayed in Figs 3a-c. It should be noted that these data include accidents caused by all kinds of hazards, and that the effect of human errors which cause deficient strength and accidental load, predominate.

The mentioned data refer to world-wide operation. In a given application it may be necessary to introduce modifications to distinct between accident rates in different geographical areas.

It can be inferred from Fig. 3c that the probability of total loss of fixed platforms world-wide is about 4×10^{-4}. Figs 3a and 3b similarly indicate the likelihood of fatalities and environmental damage. Based on the experienced likelihood of different consequences a judgement on acceptable values may be made. Having established a measure of target probability of fatalities, environmental damage and property loss, the target probability for structural failure modes may be obtained by relating these failure modes to the above mentioned consequences. This principle is further exemplified by Moan (1997).

3.4 ECONOMIC CRITERIA

The only possible single measure of risk that can include all consequences, is an economic criterion. In this case the cost of failure should, in principle, include that associated with:

- repair or replacement of the facility
- delay in operation (offhire costs)
- pollution damage (including long-term effects)
- fatalities and injury

The controversial issue is obviously to put value on life, but it may be based on *compensation* obtained in courts for loss of life expectancy, see e.g. Jordaan (1988). The economic consideration may then be based on

- cost optimization, see e.g. CIRIA (1977)
- risk-weighted cash-flow, see e.g. Stahl and Lloyd (1995)

Herein, only the implication of a cost optimization shall be illustrated by a very simple example.

The initial cost is written as (Baker, 1977)

$$C_i = a - b\log_{10}\bar{p} \tag{3.2}$$

where a and b are constants, and $\bar{p}$ is the failure probability. Should the structure fail, then the cost (C_f) will be

$$C_f = (C_i + d + e + Kn_L)\cdot pvf \tag{3.3}$$

where C_i represents the cost of replacement, d the cost of downtime (lost production), e the cost of environmental damage, n_L, is the number of lives lost and K is the cost associated with a human life. The cost, e of environmental damage will depend upon the reliability of the DHSV etc., given the failure of the system above the seabed. pvf is a present value function which discounts the future risk costs to present values and depends_upon the discount rate and the service period considered.

The failure probability, $\bar{p} = p_{FT}$ *1)* for the service period that minimizes the expected costs,

$E(C) = C_i + \overline{p}\ C_f$, is approximately

$$p_{FT} = \frac{b}{2.3\left[(a + d + e + Kn_L + \lambda \cdot b\right] \cdot pvf} \qquad (3.4)$$

where λ is a factor which is of the order - log p_{FT}.

The annual target failure probability in a service life of T_s, years will be $p_{FT}/(\alpha \cdot T_S)$, where the factor α is a function of dependence of failure in each year, and will be $\alpha = 1$ if the annual failure events are independent.

It is noted that if only the costs associated with fatalities are included and n_L is expressed as $n_L = c \cdot n_r$, it is seen that p_{FT} is inversely proportional to n_r, as proposed by Flint (1976) and CIRIA (1977).

The cost-benefit analysis, which is outlined above, can be refined by including the expected costs of injuries, limited structural damages - which may imply shutdown etc.

The cost-benefit analysis as indicated above, may be criticised on the following grounds:

- the approach is sensitive to the method in which the failure probability p_F is estimated, and the fact that p_F, should be "real" probabilities and not notional values
- the optimum safety should be established considering the total system and all hazards, including accidental events - and not be limited to the jacket (substructure) and "normal" functional and environmental hazards.
- the approach is sensitive to the marginal cost of increasing the safety level as well as the expected costs of pollution and loss of production.
- the approach does not properly recognize risk aversion, i.e. relatively more restrictive safety criteria when the severity of consequences (other than fatalities) increase

It is felt that the cost-benefit analysis especially could be applied to determine target safety levels when failure only implies economic losses. Economic risks, which only affect the (oil) company, can certainly be dealt with by the company. If potential losses affect the national economy, the authorities would decide about the acceptable risk level in the same way as for risks of life and environmental damage.

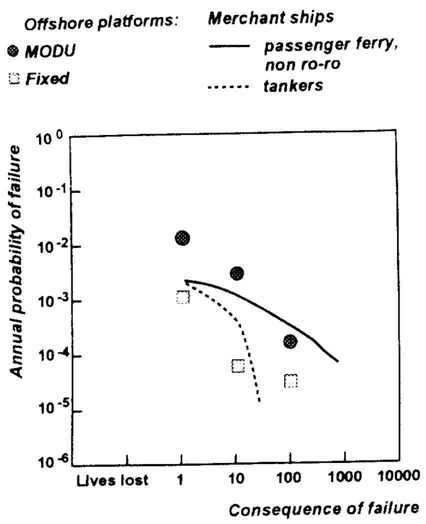
Cost-benefit analysis is also considered useful to support decisions regarding the relative safety of new versus existing structures, as illustrated in Chapter 4.

3.5 CASE STUDIES

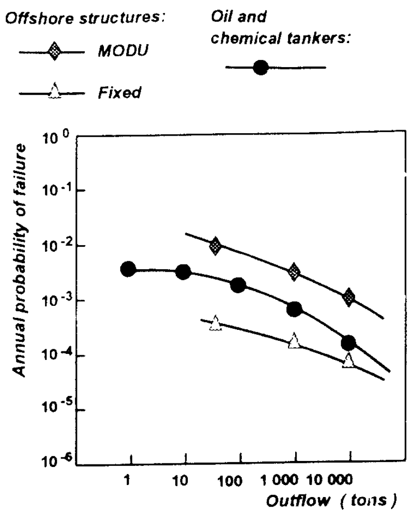
The use of the methods outlined above will be discussed with reference to target levels for:

- ultimate limit states of jacket components
- fatigue limit states of components

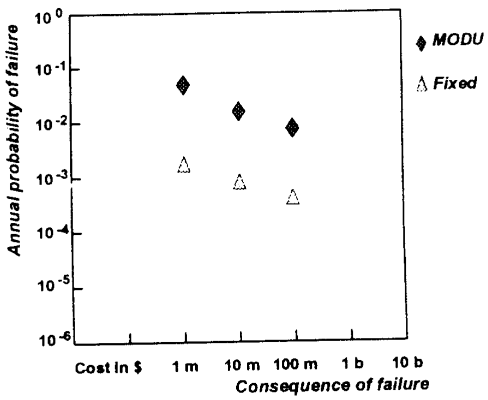
- global ultimate limit states, considering overload and fatigue failure modes of component



a) Fatality risk for platforms and merchant ships



b) Risk of outflow from offshore platforms and oil and chemical tankers



c) Risk of property loss for offshore structures

Figure 3
Frequency-consequence diagrams for offshore and ship structures in world-wide operation, considering all hazards. Historical data compiled by Moan (1998) based on data for offshore structures (WOAD, 1990) and data for ships (Aldwinkle, 1990), for the period 1980-89.

4.TARGET LEVEL FOR ULTIMATE LIMIT STATES OF JACKET COMPONENTS

4.1 CODE CALIBRATION

Ultimate limit states for structural components (notably: the partial safety factors) are calibrated based on

- defined scope of the code (class of structures, load cases, failure modes etc.)
- adequate uncertainty measures for load effects and resistances
- relevant method of reliability analysis
- properly established target reliability levels

Previous code calibration efforts are reviewed by Moan (1995).

A main issue in code calibration has been the assessment of uncertainties, especially in the environmental loads. It is debatable whether the uncertainty measures used to infer target levels implied by a given code, should refer to data available at the time the relevant code was made or whether the best uncertainty estimates should be used. For instance old data would often imply a higher uncertainty level in yield strength and geometrical imperfections than current data, due to improvement in fabrication procedures achieved in later years.

The target level in code calibration is commonly based on the average failure probability implied by some existing code, possibly with some modification. With reference to the authoritative calibration studies mentioned above, some issues relating to the effect of uncertainty measures and reliability methodology on the estimated failure probability level and hence the target level are first highlighted in Sections 4.2 and 4.3. Implied failure probability levels in authoritative code calibrations are mentioned in Section 4.4. Finally, target levels are discussed in Section 4.5.

4.2 UNCERTAINTY MEASURES

In this section a brief summary of uncertainties relevant for load effects and resistances in jackets is provided, based on the more extensive survey by Moan (1995).

For jackets the uncertainties in load effects due to sealoads, wind, ice, earthquake ... predominate. They depend upon uncertainties in the environmental conditions as well as, force and response calculation. The uncertainty in environmental load effects for jackets with quasi-static behaviour, may be determined based on the following model:

$$S = \psi\, C \cdot E^{a} \tag{4.1}$$

where ψ - random model uncertainty
C - coefficient determined in regression analysis
E - characteristic environmental parameter

Eq. (3. 1) has typically been applied to model sea loads on jackets based oil the design wave method (with α typically in the range 1.3 to 2.4), here E represents a wave height. The bias of E refers typically to the ratio of expected annual maximum and the characteristic value used in the equation.

It is noted that Eq. (4.1) provides a satisfactory representation of the load as long as the wave crest does not reach the platform deck. To account for the fact that the wave may reach the deck at a wave height of E, the following formulation may be applied

$$S = \psi \cdot C \cdot E_c^{a} + \psi' \cdot C'(E - E_c)^{\alpha'} \quad for \quad E \geq E_c \tag{4.2}$$

here ψ' and α' represent a random model uncertainty and a constant, respectively.

The effect of different environmental climates, i.e. tropical vs. extratropical climate - on the inherent uncertainty of the environmental condition, specified by wave height and period, current velocity and the water depth (water level) is especially noted. Also the difference in approaches used to collect and implement data for these different wave climates contribute to differences in uncertainty level (Moan, 1995).

The uncertainty associated with the basic environmental data is commonly assumed to be negligible due to efforts made to avoid measurement errors etc. The statistical uncertainty of annual maximum significant wave height H_{mo} e.g. for extratropical areas with "continuous" storms, corresponds to a COV of 0.16. If the annual maximum individual wave height H is inferred from this H_{mo} further uncertainty occurs. The COV is smaller (approximately 0.07 - 0.08) for the annual maximum H_{mo} when using the long-term distribution of H_{mo}. It is noted, however that the expected annual maximum is larger in the latter case. Both measures have been used in code calibrations (Moan 1995).

Extreme sealoads on jackets with a static behaviour are universally estimated based on a regular design wave and Morison's equation, involving the following main features:

- wave height and corresponding period, and a current velocity
- wave particle kinematics and current velocity profile
- values of hydrodynamic coefficients

Two main procedures are currently used to estimate the sea loads on jackets. They include the wave sequence API RP 2A (1993) and so-called standard North Sea practice, which differ with respect to wave kinematics, hydrodynamic force coefficients, as well as in treatment of current blockage and shielding effects. The model uncertainty (ψ in Eq. 4.1) of the API load recipe has been determined by comparison with full-scale measurements, yielding a bias 1.06 and a COV of 0.25 (Heideman and Weaver 1992). In the initial API calibration (Moses, 1986, 1987) a different bias of 0.93 was used. In an effort (Turner et al, 1992; Theophanatos, 1992) to develop a European API LRFD design procedure a more refined load model, maintaining two terms in Morison's equation, was used. However, the uncertainties were assigned to the wave height, hydrodynamic coefficients and current velocity-resulting in total load uncertainties corresponding to a bias and COV in the range 0.71 - 0.88 and 0.19 - 0.29, respectively. The total COV seems to be slightly small. Other studies have been based on similar uncertainty models.

Static load effects for ultimate strength design of jackets are generally determined by means of a finite element frame model. The main uncertainties involved are associated with the flexibility of tubular joints and pile-soil interaction.

At a given site, current and wind conditions do not necessarily occur at the same time or in the same direction. The metocean criteria in API RP2A permit the designer of a new platform to take full advantage of this fact. The guidelines of HSE in the UK, and the NPD regulations recognize this fact, but do not allow the designer to take advantage of it. However, this conservatism is partly compensated by the choice of hydrodynamic force coefficients.

Resistances of compression members, beam-columns and tubular joints are used in design and reliability assessment of jackets. For tubular members the bias and COV of the ultimate strength is typically 1.0-1.1 and 5-10%, respectively, implying a relatively small variation between different approaches. For unstiffened simple tubular joints the uncertainties vary much more (see e.g. Moan 1995).

4.3 RELIABILITY ANALYSIS

While FORM and Monte Carlo type methods (e.g. Melchers, 1987) represent the state of the art approaches for reliability estimation, simple formulations have often been used in code calibrations. In general, the failure probability, p_F may be written as

$$p_F = \Phi(-\beta) \tag{4.3}$$

Log-normally distributed resistance R and load effect S are frequently assumed. Sometimes the corresponding safety index, β_{LN} is approximated by:

$$\beta_{LN} = \frac{\ell n\left[\frac{\mu_R}{\mu_S}\sqrt{\frac{1+V_S^2}{1+V_R^2}}\right]}{\sqrt{\ell n\left[(1+V_R^2)(1+V_S^2)\right]}} \approx \frac{\ell n(\mu_R/\mu_S)}{\sqrt{V_R^2+V_S^2}} = \beta_{LN}^{'} \tag{4.4}$$

where μ and V denote mean value and COV respectively.

The calibration of the API LRFD jacket Code (Moses 1986; 1987) in USA was based on a log-normally distributed resistance and load effect. In the CSA work (Maes, 1986) the resistance was taken to be log-normal while different probabilistic models were applied for the loads. The calibration of ULS criteria by Fjeld (1977) was based on log-normal and normal distributions for resistances and load effects, respectively.

To illustrate the effect of the distribution tail on the reliability level consider the following two models for the resistance and load effects

- log-normal for both resistance and load effect
- log-normal for resistance and normal for load effect

Fig. 4 shows how the reliability index corresponding to these two models relate to each other. With typical coefficients of variation V_R = 0.15 and V_S = 0.3 and β_N in the range 3 to 4, β_{LN} will vary in the range 2.6 to 3.3.

This fact shows that the target level is intimately associated with the reliability model applied.

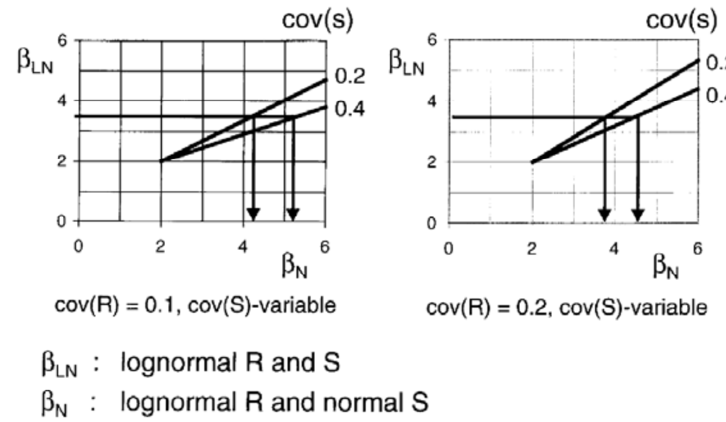


Figure 4
Comparison of reliability index, β based on log-normal R and S (β_{LN}) and log-normal R and normal S (β_N).

In calibration studies considered herein, the reliability analysis has been based on a load formulation similar to Eq. (4. 1). If *the airgap is sufficiently large, this assumption is justified.* However, if the *design value of* the wave height, E (as determined in the reliability analysis) exceeds E_c, Eq. (4.2) should be used to obtain a satisfactorily accurate estimate of the reliability. The practical implication of this feature is that an airgap on the 100 year crest of the order 1.0-2.0 m would be required to achieve annual failure probabilities of the order 10^{-3}, while the airgap needs to be 2.0-3.0 m to achieve a p_F of 10^{-4}: depending upon the magnitude of wave height, relative magnitude of resistance and load uncertainties. Using Eq. (4.1) for cases where Eq. (4.2) is the correct formula may, obviously, lead to significant errors in predicted $p_{F's}$.

4.4 PROBABILITY OF COMPONENT FAILURE IMPLIED BY ULS CRITERIA

The component probability of failure inherent in the relevant design code is obtained by using relevant measures of uncertainty and reliability methodology - i.e. consistent with the approach to be used to demonstrate compliance with the target level. The emphasis should be placed on primary components in the relevant type of structure and foundation.

Previous studies of implied annual failure probabilities associated with ULS criteria for offshore structures, are reviewed by Moan (1995). The values for jackets typically vary in the range $2x10^{-3}$ to 10^{-5}, depending on the design code formulation, uncertainty measures and reliability methodology which is applied.

4.5 TARGET LEVELS

The calibration of the API Code was undertaken using the same average target reliability as implied by the previous API Codes and, hence, primarily aimed at a more uniform reliability level. A similar approach has been used in most calibrations so far. However, in the Canadian effort mentioned above, the target level was explicitly assessed (Jordaan and Maes 1991) based on cost analysis (minimum total costs, including expected damage costs) and acceptable fatality risk for individuals. Jordaan and Maes only found the latter approach appropriate and proposed, assuming that a component failure implies fatalities, that the annual target component failure for probability; should be approximately 10 % of the annual fatality rate in the society.

If the same uncertainty measures and reliability methodology is used when determining the implied failure probability in an existing code (to establish the target level) as used when calculating the inherent failure probability in the new code, the sensitivity of the partial safety coefficients to the data and methods will be limited. As indicated in Section 4.2 different uncertainty measures are used by different researchers and organizations. The approach may hence not be consistent if target levels inferred by one organization is applied by another. Target levels should be tied together with specified procedures for uncertainty and reliability analysis. Moreover, it is important that the reliability-based design is consistent with well established design practice for commonly occurring cases. Unfortunately, general guidance in NKB (1978, 1987), DNV (1995) and other sources, typically specifying annual target values in the range 10^{-3} to 10^{-6}, is not consistent with reliability measures quantified for different codes for various types of offshore structures, as demonstrated by Moan (1995), and can in general not be recommended for use.

5. TARGET LEVEL FOR FATIGUE LIMIT STATES OF COMPONENTS

5.1 GENERAL

The fatigue strength endurance under random loading is commonly described by SN-curves and the Miner-Palmgren (MP) hypothesis. In as-welded structures - the residual stress adjacent to welds can be assumed to be tensile and amount to the yield level. In this case the stress range is the only load effect parameter. The fatigue loading may be conveniently described by a two-parameter Weibull distribution. For welded structures the SN-curves are independent of yield strength, because the life times refer to crack growths, which are independent of yield strength. For base material or notched material the SN curve will depend upon the yield strength. The high cycle range of the SN curve depends upon the corrosive environment. The two major fatigue codes used for offshore structures today are those of the HSE (1995) and API (1994). SN-curves typically refer to the fatigue endurance from a small initial fabrication defect to a through thickness crack. Fracture mechanics (FM) analysis may especially be used to determine the time to specified crack sizes and especially the residual fatigue life - e.g. from through-thickness crack to final fracture.

5.2 SN-APPROACH

The basic design equation based on the SN-MP approach, may be written as:

$$D = \sum_i \frac{n_i}{N_i} \le \Delta \tag{5.1}$$

Assuming that the stress range (and not the mean stress) characterizes the fatigue strength and using the SN-data according to $N = KS^{-m}$ and the Weibull distribution for the stress range, the long-term cumulative damage may be written as, see e.g. Chapter 10 of Almar-Næss (ed.) (1985),

$$D = \frac{N_o}{K}\left[\frac{S_o^B}{\ell n N_o}\right]^{m/B} \Gamma(m/B+1) \tag{5.2}$$

where:

N_o	-	total number of cycles in the long-term period considered
S_o	-	the expected maximum stress range in N_o cycles
B	-	shape parameter of the Weibull distribution for the stress cycles (Note that the scale parameter, λ for the Weibull distribution is $A = S_o/(\ell n N_o)^{1/B}$
K, m	-	parameters for the SN-curve; with $K = K'(22/t)^{m/4}$, where K' is the reference parameter and t the plate thickness in millimetre

It is interesting to note that the maximum damage in a stress range interval, ΔS occurs for an S at a level of $S = S_o\left[(m+B-1)/(B \cdot \ell n N_o)\right]^{1/B}$, which normally amounts to about 25% of S_o.

5.3 CALIBRATION OF FLS CRITERIA

Code calibrations described in Section 4 refer to ultimate strength criteria. Because fatigue in offshore structures is mainly caused by one type of loading, there is limited merit of calibrating fatigue criteria in the same sense as ULS criteria, relating to multiple loads. However, to achieve consistent design and inspection criteria fatigue design criteria should be calibrated to reflect the consequences of failure and inspection plan.

Another issue regarding calibration is associated with alternative reliability formulations, in terms of the SN-MP approach or a fracture mechanics (FM) method. Only the latter approach can generally deal with reliability estimates relating to cracks of any size and, hence, the only feasible method to update reliability based on inspection results. However, since the FM approach is sensitive to initial crack sizes, this method should be calibrated both deterministically and probabilistically to the SN-MP approach for cases where both are applicable.

Finally it is noted that e.g. the NPD Regulations (1992) actually require methods beyond conventional design methods to be shown to yield a design comparable to that obtained by conventional methods, to be acceptable.

5.4 UNCERTAINTY MEASURES

Uncertainty measures for probabilistic fatigue analysis based on the SN- and FM approaches are briefly discussed by e.g. Moan (1997), relating

- resistance (SN data; and fracture mechanics model and parameters; respectively)
- load effects (hot spot stress)

5.5 RELIABILITY METHODS

5.5.1 General Methods

Methods described in Section 4.3 can in general be applied to calculate the fatigue failure probability.

5.5.2 SN-Approach

A simple estimate of the p_F may be centred around common design practice by adopting the SN - Miner Palmgren approach. The failure function is then given by

$$g(\Delta, D) = \Delta - D \tag{5.3}$$

where D is for instance approximated by Eq. (5.2).

By adopting log-normal distributions for Δ and D in Eq. (5.3), the failure probability is given by Eq. (4.3) with

$$\beta = \frac{\ln(\tilde{\mu}_\Delta / \tilde{\mu}_D)}{\sqrt{\ln(1+V_\Delta^2)(1+V_D^2)}} \approx \frac{\ln(\mu_\Delta / \mu_D)}{\sqrt{V_\Delta^2 + V_D^2}} \tag{5.4}$$

where $\tilde{\mu}$ is the median value. This formulation was used by Wirsching in the first efforts on probability-based fatigue design criteria for offshore structures (Wirsching, 1983).

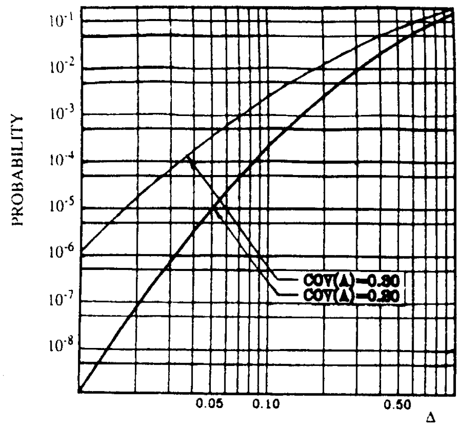
The expression (5.3) then refers to the p_F in N_o cycles that occur in a given time period, t.

The effect of the fatigue design criterion on p_F can be illustrated by the simple example described by Moan (1990), based on Eqs (5.2, 5.3) and the following assumptions on uncertainty measures:

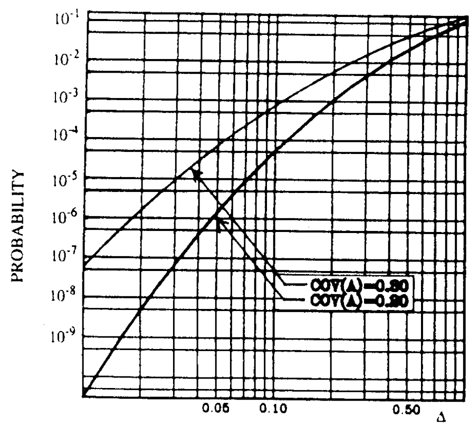
SN-curves:	DEn C and F curves with mean and standard deviation as given in the code. m is assumed to be deterministic.
Stress cycles:	Weibull distribution with - scale parameter, A, determined to fulfil the design equation, and a coefficient of variation equal to 0.2 or 0.3 - shape parameter, B with mean value equal to 1.0 and V equal to 0.05
Miner sum:	Deterministic value equal to 1.0

It is noted that the variables A and B should reflect all uncertainties in load effects, i.e. due to hydrodynamic and structural model as well as wave height.

Selected results of the fatigue analyses are shown in Figs 5a-b. It is noted that practically the same results are obtained when using SN-curves E through G (which all have m = 3.0).



a) Joint class C.



b) Joint class F.

Figure 5
Probability of fatigue failure in the service-life as a function of allowable fatigue damage, Δ for welded joints according to DEn. A and B are the scale and shape parameters of the Weibull distribution of stress cycles (Moan 1990).

With a COV(A) between 0.2 and 0.3 and COV(B) = 0.05 and an allowable Δ between 0.06 and 0.15 it follows that p_F is $6x10^{-4}$ (approximately corresponding to an annual probability of overload failure of about 10^{-4}). This is the initial justification for using an allowable Δ of 0.1 in the NPD Regulations (1992) for joints when failure consequences are significant and the joint can not be inspected.

5.5.3 Fracture Mechanics Approach

Based on fracture mechanics the failure function for fatigue in the period t may be written as:

$$g(\underline{z}) = a_f - a(t) \leq 0 \tag{5.5a}$$

where a_f and a(t) are the crack size at failure and time t, respectively. a_f and a depend on various material and geometrical parameters, $\underline{z}$.

Alternatively , the limit state function g( ) may be written as:

$$g(\underline{z}) = T_o + T - t \tag{5.5b}$$

where the time, T from an initial crack size, a_o to failure (crack size) can be obtained by integrating Paris' law

$$da/dN = C \cdot (\Delta K)^m = C \cdot \left[S \cdot Y \cdot (a, \underline{P}) \cdot \sqrt{\pi a}\right]^m \tag{5.5c}$$

as follows:

$$T = \frac{1}{C \cdot v_o \cdot E(S^m)} \int_{a_o}^{a_f} \frac{da}{\left[\gamma_Y \cdot Y(a, \underline{P}) \cdot \sqrt{\pi a}\right]} \tag{5.5d}$$

where ν_o is the mean zero upcrossing frequency of S, and $E(S^m)$ is expectation of S^m . γ_Y *is* the model uncertainty of Y( ), and $\underline{P}$ is a set of parameters.

The failure probability, p_F (t) can be calculated by FORM/SORM, using the failure functions given above.

Furthermore, because fatigue and fracture control relies on design as well as inspection and repair, much work has also been devoted to investigating the effect of inspection on the reliability.

Updating of the failure probability can be made using the Bayesian approach e.g. by:

$$P\left[g(\underline{z})0 | \mathrm{I}\right] = \frac{P\left[(g(\underline{z}) \leq 0) \cap (\mathrm{I})\right]}{P[\mathrm{I}]} \tag{5.6}$$

where g ($\underline{z}$) is the failure function and the event I symbolically expresses the relevant outcome of one or more inspections.

The failure probability mentioned above was referred to a time period t. The probability of failure in an interval t to t + Δt may be expressed as

$$P[t \le T \le t + \Delta t] = f(t) \cdot \Delta t \tag{5.7}$$

in which

$$f(t) = dp_f(t) / dt \tag{5.7a}$$

Alternatively, the failure or hazard rate (function) h(t) may be determined as the likelihood of failure in the time interval t to t + Δt and Δt $\rightarrow$ 0, given that failure has not occurred prior to the time t. The failure or hazard rate is then calculated by:

$$h(t) = \frac{dp_f(t)/dt}{l - p_F(t)} = \frac{\phi(-\beta(t))}{1 - \Phi(-\beta(t))} \cdot \frac{d\beta(t)}{dt} \tag{5.8}$$

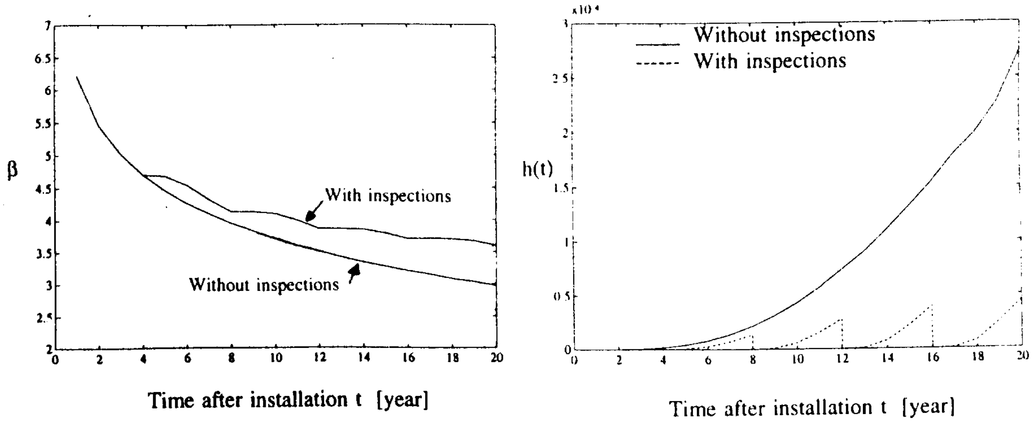
It is noted that

$$h(t) = f(t) / \left(\int_t^{\infty} f(u)\, du \right) \tag{5.9}$$

Since the denominator in Eq. (5.9) is equal to 1 - $\Phi(-\beta(t))$, the difference between h(t) and f(t) is generally small if β is larger than, say 2.0.

Fig. 6 shows an example, illustrating how β and h(t) vary with time, t for a welded joint. The effect of inspection every 4th year is estimated by information available at the design stage. The outcome of the inspection, i.e. detection or no detection of a crack is not known at the design stage, but estimated by available information. It is observed that the β for the service life, t = 20 years is increased from approximately 3.0 to 3.5 by the inspection. More importantly, the inspection makes the hazard rate much more even over the service period. It could even have been more even by scheduling inspections more and more frequently towards the end of the service life.

In the example in Fig. 6b with no inspection the average and maximum annual hazard rates are about $1.0x10^{-4}$ and $2.5x10^{-4}$, respectively, while the failure probability in 20 years is about $2x10^{-3}$, It is noted that the failure probability in 50 years will be the same if the allowable fatigue damage is the same as for the case in Fig. 6. However, the average annual hazard rate in this case will be only 40% of that for the case in Fig. 6. Hence, for cases with the same failure probabilities for different in-service lives, the annual failure rates differ. This aspect should be noted when establishing target levels.



a) Reliability index, β(t). b) Hazard rate, h(t).

Figure 6
Reliability measures for a welded joint with mean initial and as-repaired crack size of 0. 11 mm and mean detectable crack size of 1.5 mm. Inspections are assumed to be carried out every fourth year, with outcomes, crack detection (land repair) and no crack detection, are estimated at the design stage.

5.6 TARGET RELIABILITY LEVELS

5.6.1 General

In the following section various ways to establish the target reliability level for fatigue failure of components are outlined.

A better approach would be to use the failure probability implied by adequate existing fatigue requirements as target value. More generally the methods outlined in Section 3 can be applied. However, if ULS criteria are derived by any of these methods, fatigue criteria may be calibrated to the same reliability level as ULS criteria, when no inspection is applied to control the cracks and the failure consequences are comparable. It is then necessary to refer the two probabilities to the same reference time, i.e. a year or service time (i.e. 20 or 100 years).

It is preferable only to use design criteria which are not based on some kind of non-destructive examination (inspection). This is because the interrelation between design and inspection requirements currently are quite vague. For instance, fatigue criteria by NPD (1992) only accounts for the effect of whether there is access for inspection or not, with no consideration of the frequency, method and quality of inspection.

Finally, it is important to ensure consistency in the methodology used to calculate the probabilities implied by ULS and FLS criteria.

In the subsequent sections some further comments on consistency in

- failure consequences
- reference period for the failure probability
- reliability methodology

are provided.

5.6.2 Failure Consequences

While ULS criteria usually refer to truly ultimate limits of bearing capacity, fatigue commonly refers to partial failure as mentioned in Section 2.3. Even if target levels for FLS inferred from ULS criteria for this reason may be relaxed, fatigue-induced fracture should be noted. If there is no explicit criterion to accommodate occurrence of a fracture that may result from an overload of a component deteriorated by fatigue cracks, a conservative definition of the fatigue limit state should be adopted.

5.6.3 Reference Period for Failure Probability

As mentioned in Section 2.4, the target level should refer to

- annual probabilities when fatalities may result
- service life or some other long-term period

The reference value inferred from ULS criteria can be readily obtained in terms of annual or service life values. For situations with no strength degradation and stationary load processes, the relevant failure probability may be calculated by taking the load effect to correspond to the maximum value in the given reference period.

While fatigue failure probabilities as mentioned in Section 5.5 commonly are referred to the service life, the hazard or failure rate, h(t) or even the probability density of p_F can be easily determined as discussed in Section 5.5.3. The latter probability measures allow comparison with annual target levels. However, it is noted in Fig. 6 that h(t) varies considerably over the service life when no inspection is carried out. In this case the target level may be specified as the maximum or mean annual failure probability. It is only a maximum annual hazard rate that is consistent with the idea of limiting the p_F value for each year.

More specifically the following procedures may be used to establish the target levels for fatigue.

An implied p_F for the service life, which is obtained from fatigue criteria based on the SN-approach, can be used directly as target value for the service life, possibly with adjustment to account for different consequences. This p_F may also be easily transformed to an average (or maximum) annual failure probability, when p_F is known as a function of time.

Similarly, an annual failure probability implied by ULS criteria may be used directly as basis for the annual target failure probability. The service life target value may be obtained by transforming the annual reference value to the relevant time period. Based on this value, the average annual failure probabilities may be determined.

5.6.4 Reliability Methodology

As mentioned above, consistency between the reliability methods applied to determine the target p_F and the p_F that should comply with the target value, should be aimed for. This is a particular challenge if failure probabilities associated with ULS (overload) are used to infer the target level for fatigue. In this case the consistency can be ensured to a limited extent because the resistance and load formats are different for different limit states. It is important that the nominal load effects for ULS and FLS are described in terms of a long-term distribution and consistent uncertainty levels are assigned to the load effects at different exceedance levels, i.e. for extreme and fatigue load effects.

6 TARGET LEVEL FOR GLOBAL FAILURE

6.1 GENERAL

In this section the target safety level for global failure of jackets under sea loads is addressed.

Target levels for the system may be established based on the general principles outlined in Sections 2 and 3. However, only approaches based on some kind of calibration with existing design criteria will be pursued in this context. More copious methods would be difficult to get accepted. This is because a probabilistic system approach represents a double step beyond current practice, based on semi-probabilistic, component design checks.

Ultimate and fatigue design criteria in current design codes are primarily based on component failure modes (limit states) and a linear global model of the structure to determine the load effects in the components. However, some codes, notably the NPD Regulations (1992), contain progressive collapse and fatigue design criteria, which have implications on systems failure probability levels. Moreover, recently ULS criteria for the system have been introduced in connection with assessment of existing structures.

An approach which is based on global (system) failure modes of the structure, is desirable because significant consequences, e.g. fatalities will primarily be caused by global failure. A proper systems approach is also necessary to obtain the optimal balance between design and inspection plan since it is normally based on a certain damage tolerance, especially when the inspections rely on detecting flooded or failed members.

The prediction of ultimate behaviour of framework structures like jackets is complex. However, recent developments of efficient methods which account for large deflections and plasticity and even premature fracture (e.g. Moan and Amdahl, 1989, Ueda and Rashed, 1991 and Stewart et al., 1993) facilitate systems reliability approaches. Significant progress has been made in reliability analysis of jackets over the last 5 years.

In the systems approach the relevant structure is assumed to be composed of different physical components (members, joints, piles, ...) which each may have different failure modes, e.g. different collapse, fracture or fatigue modes. In general global failure results after a sequence of failure modes for different components.

Another important feature of structural systems is that failure may occur in a large number of modes. For instance, system failure may be initiated by fatigue failure of a large number of joints. This feature is especially notable in a long member with n possible fatigue crack sites under the same load conditions. Depending upon the correlation of loads and resistances at the different sites the failure probability will be bounded by one and n times the failure probability of a single site. This "series system " issue is currently not explicitly addressed in design codes.

Finally, it is again noted that target levels should be derived in a manner which is consistent with the calculation of the reliability levels which should be shown to comply with the target levels.

In Section 6.2 calculation of the failure probability of the system is briefly described. In the subsequent sections the implication of systems (target) reliability by various design criteria will be examined, considering;

- p_F implied by ULS criteria for components, with an adjustment to be relevant for the systems (Section 6.2)
- p_F implied for the system by NPD PLS (now: ALS) requirements (NPD, 1992) (Section 6.3)

- p_F implied for the system by NPD FLS criteria (Section 6.4)

In Section 6.5 it is shown that the target level for economic and social reasons may be relaxed acceptable (failure probability increased) for existing structures compared to new ones.

6.2 FAILURE PROBABILITY OF STRUCTURAL SYSTEMS

Having established the limit state $g_{i_j}^{(...)}(\quad)$ for all events in each system failure mode and uncertainty measures for all random variables, the failure probability may be calculated by

$$p_{FSYS} = P[FSYS] = P\left[\bigcup_{i=1}^{N}\bigcap_{j=1}^{ni}(g_{i_j}^{(...)}(\quad) \leq 0)\right] \tag{6.1}$$

by FORM/SORM, bounding techniques or simulation methods. Due to the significant effort involved, it is important to apply some kind of technique to limit the number (N) of failure modes.

Fortunately, accurate estimates of the systems failure probability for jackets under extreme sea loading can commonly be achieved with a very simple model, corresponding to a single systems failure mode, i.e. by referring both the load and resistance to a given load pattern and using the (overall) base shear as variable. This model has been validated for cases where the load uncertainties are dominant and the component forces are highly correlated (Wu and Moan, 1989, and De et al., 1989).

This approach may be extended to include fatigue failure modes, using the basic overload case as the reference case. A variety of failure sequences should then in principle be considered. A first approximation to p_{FSYS}, considering both overload and fatigue failure modes, may be achieved by

$$p_{FSYS} = P[FSYS] \approx P[FSYS(U)] + \sum_{j=l}^{n} P[F_j] \cdot P[FSYS(U)|F_j] \tag{6.2}$$

where FSYS(ULS) is the overload system failure and F_j represents fatigue failure of component j. It is noted that the first term then accurately covers all pure overload failure sequences. The main approximation is the negligence of sequences initiated by component overload, followed by component fatigue failure or sequences initiated by fatigue failure and followed by more fatigue or overload failures. Consideration of only a few of many alternative component failure sequences is obviously non-conservative. On the other hand neglecting the correlation between various sequences is conservative.

If the individual products in the sum in Eq. (6.2) are approximately equal, the contribution from the last term in Eq. (6.2) may be significant. In most cases, however, a few contributions will be dominant.

The failure probability p_{FSYS} (Eq. (6.2)) may most conveniently be referred to service life or alternatively to a period of a year. P[FSYS(U)] for the service life is estimated by the probability of a union of annual failure events. P[F_j] is then the probability of fatigue failure in the service life. P[FSYS(U)[F_j] in principle should be calculated as the probability of overload failure in the remaining time of the service life after fatigue failure. However, if it is assumed that inspection is carried out and that fatigue failures (of the complete member) can be reliably detected, e.g. by an annual visual inspection, the latter failure probability may be calculated as an annual overload probability.

If p_{FSYS} is estimated as an annual probability, the fatigue failure event F_j could be split into mutually exclusive events F_{jk} which denote fatigue failure in year k, given survival up to that time. The conditional probability of FSYS(U) given F_{jk} is then calculated as the probability of overload failure in the period from year k to the end of this service life. Alternatively, if inspections with reliable detection of member failure is assumed, the probability of overload failure could be referred to an annual value.

It is interesting to note that Eq. (6.2) provides a basis for comparing the contribution to the failure probability by systems overload failure and a fatigue-induced system failure.

6.3 SYSTEM FAILURE PROBABILITY IMPLIED BY COMPONENT ULS CRITERIA AND A SYSTEMS MODIFICATION FACTOR

The relevant target level is then established by

- using the implicit component reliability in the relevant design code for new structures as a reference failure probability
- requiring that the system reliability (failure probability) is higher (lower) than that for components

This approach is detailed in the following section.

A target reliability measure for components may be obtained as the implied failure probability level for components as shown in Section 4. This probability level needs to be adjusted to serve as a target level for the system.

Since most design codes do not specify systems safety requirements, not even in a deterministic sense, statically deterministic structures are permitted. In this case the system failure probability will be equal to or greater than the maximum component failure probability. However, in most cases jackets and other structures have a global ultimate strength, which exceeds the load which corresponds to γ times the load that gives first component failure. This reserve capacity may be explicit or implicit overdesign, e.g. by using conservative methods for load and resistance calculation, by using materials with reserve strength, and the fact that pre-service loads and requirements to buoyancy or space for piles in legs may increase member sizes (of platform legs) beyond that required for in-service strength. Such reserve strength is most likely to be present in a system with "redundant" components. For six jackets on the Norwegian Continental Shelf first member failure was found (see e.g. Stewart et al. 1993) to occur at a load which is 2.04 to 3.23 times the characteristic ("100 year") value. This implies an "overdesign factor" of 1.35 to 2.15 on the minimum reserve capacity based on a total safety factor $\gamma = 1.15 \cdot 1.3 \cong 1.5$ on environmental loads. System collapse occurs at a load, which typically was a few percent larger than the load at first member failure. It is hence noted that the high reserve strength in most cases is facilitated through very limited non-linear large deformation, elasto-plastic load behaviour.

The reserve strength is affected by the aspects mentioned above. Finally, it is noted that the "overdesign" factor varies significantly, since it is not implied by consistent direct design requirements.

When setting target reliability levels, it is felt that the effect of the "indirect criteria" mentioned above that contribute to the reserve strength, should not affect the target level. Hence, in principle the target level could be taken to be the reliability level implied by a statically determinate jacket designed to perfectly fulfil design criteria for in-place condition, which is a permissible concept. Now, considering other concepts, like the hull girder of ships, monotower concrete platforms, experiences suggest that there is normally a small reserve capacity in any type of structure, beyond the "component limit state".

The hull girder of a production ship possesses reserve strength beyond first component failure of typically 10- 15%, for a reasonably well balanced longitudinal strength design (Moan 1988; Frieze et al. 1991). Cylindrical prestressed concrete monotowers, are also expected to have a reserve ultimate strength beyond the design limit state; at most up to 1.27 (in bending). However, in this case it may be questioned whether the truly ultimate limit state with respect to strength is relevant, because excessive cracking of the concrete will occur before the strength limit state is reached. Other, more complex framed fixed and floating structures may have higher system reserve capacity. However, this is a result of other considerations and the choice of system.

On this background it is felt reasonable to establish a basic target level based on a system strength which is 1.15 times the load which causes first component failure in a statically determinate system. This strength ratio corresponds to an increase of the safety index by 0.3-0.4 units or a target probability for the system which is (α_{CS} = 1/3-1/5 of the component level, for typical reliability levels.

The procedure for setting target level for overall system failure could be refined by accounting for the post-ultimate behaviour as mentioned in Section 2.3.

The system failure probability thus established as a target level represents a conservative value, because it only relates to the first term in Eq. (6.2) associated with ultimate failure under overload. The total target value should include the second type of terms in Eq. (6.2), basis on implied systems failure probability due to fatigue failures, as discussed subsequently in Section 6.5. In the next section the implication of PLS criteria on the target level for global ultimate overload failure, is assessed.

6.4 SYSTEMS FAILURE PROBABILITY IMPLIED BY NPD PLS CRITERIA

The progressive collapse limit state (PLS) by NPD (1992) was introduced to ensure global safety in connection with accidental and other loads, Q_{10000} corresponding to an exceedance probability of 10^{-4} per year. The PLS criterion means that the (characteristic) global ultimate strength $R_{sys(c)}$ should be at least equal to the global Q_{10000} sea load. This ULS criterion implies a certain (target) system failure probability.

The failure probability implied by component ULS criterion and the mentioned PLS criterion may be compared by using Eqs (4.3, 4.4) as the probabilistic model, and Eq. (4.1) to model the environmental load effect, with E = H (wave height). Permanent and live loads are neglected without loss of generality if the braces are the critical components.

Consider first the conventional ULS check of components. R and S then refer to members or joints.

The mean maximum annual environmental load effect may be written as

$$\mu_S = \mu_\psi \cdot C \cdot H_1^\alpha \tag{6.3}$$

where H_1 the annual maximum wave height.

According to the design check: $R_c / \gamma_R = \gamma_S \cdot S_C$ the required characteristic strength is

$$R_c = \gamma_R \gamma_S C H_{100}^{\alpha} \tag{6.4}$$

It is noted that the model uncertainty in load effects is not accounted for in the design check.

The corresponding mean resistance is then

$$\mu_R = B_R R_C = B_R \gamma_R \gamma_S C H_{100}^{\alpha} \tag{6.5}$$

where B_R is a bias of the resistance.

The COV's of R and S are denoted by V_R and V_S. V_S is given by:

$$V_S \approx V_{\psi}^2 + \alpha^2 V_{H_1}^2 \tag{6.6}$$

where V_{ψ} represents the model uncertainty of the load effect.

Using the simplified log-normal reliability format the implied component β *is:*

$$\beta' = \frac{\ln(\mu_R / \mu_S)}{\sqrt{V_R^2 + V_S^2}} = \frac{\ln\left[\dfrac{B_R \gamma_R \gamma_S}{\mu_{\psi}}\left(\dfrac{H_{100}}{H_1}\right)^{\alpha}\right]}{\sqrt{V_R^2 + V_{\psi}^2 + \alpha^2 V_{H_1}^2}} \tag{6.7}$$

Similarly, the implied reliability index β'_{SYS} for the system corresponding to the PLS criterion becomes

$$\beta'_{SYS} = \frac{\ln\left[\dfrac{B_{R(SYS)}}{\mu_{\psi(SYS)}}\left(\dfrac{H_{10000}}{H_1}\right)^{\alpha}\right]}{\sqrt{V_{R(SYS)}^2 + V_{\psi(SYS)}^2 + \alpha^2 V_{H_1}^2}} \tag{6.8}$$

To compare the implications of the component ULS and the PLS criterion it is necessary to do it on the same basis, i.e. with reference to the systems reliability. The main issue is then that the resistance in the ULS component cheek refers to individual components and that a linear analysis is applied to determine load effects in components. Hence, the possible reserve strength due to elasto-plastic behaviour of the system is not accounted for in this case. If $B_{S(SYS)}$ is set equal to B_S, the mentioned issue may be taken into account by setting: $B_R \geq B_{R(SYS)}$.

The model uncertainty associated with the global load effect (base shear) and (local) member/joint load effects, may be assumed to be the same, since the contributions from common features (wave kinematics, Morison's equation) predominate. Moreover, since the uncertainties V_{ψ} and V_{H_1} are larger than V_R and $V_{R(SYS)}$, the latter COV's may, for simplicity, be taken to be equal.

It is seen that the component ULS criterion will determine the implied reliability level if:

$$\Gamma_{\beta} = \gamma_R \gamma_S \frac{B_R}{B_{R(SYS)}} \left(\frac{H_{100}}{H_{10000}} \right)^{\alpha} > 1 \qquad (6.9)$$

with $\gamma_R \gamma_S = 1.5, H_{100} / H_{10000} \sim 0.8$ and $B_R / B_{R(SYS)} = 1.0, \Gamma_{\beta}$ is greater than 1 for $\alpha \geq 1.82$. With B_R = 1.15 $\cdot B_{R(SYS)}$ (as indicated in Section 6.3) and the other parameters unchanged, $\alpha \geq 2.44$.

The load model (Eq. (4. 1)) applied above was assumed to involve the jacket only. This is an acceptable approximation as long as the "design value" of the wave crest as obtained as a result of the reliability analysis is below the deck. The effect of the design wave crest touching the deck will be a higher failure probability. It will, hence, be conservative to neglect this effect when the establishing the target level.

Clearly, the simplified analysis presented above may be refined by considering a more accurate physical model, reliability method, uncertainty measures etc. It is again emphasised that the method used to infer target levels should be consistent with the method applied to demonstrate compliance with the target level.

6.5 SYSTEMS FAILURE PROBABILITY IMPLIED BY NPD FLS CRITERIA

The fatigue requirements in the NPD Regulations (1992) are related to the systems strength. The acceptable fatigue damage, D depends upon access for inspection and failure consequences. If no access for inspection is possible (or inspection is not planned), the allowable D is 0.1 and 0.33 for major and minor consequences, respectively. The consequences are interpreted to be minor if the PLS criterion is fulfilled. The limiting case for major consequences is when the structure is statically determinate. For this case the allowable D is 0.1.

The probability of the system due to fatigue failure may be approximately estimated by (Moan, 1998)

$$p_{FSYS} = \sum_j P\left[FSYS(U) \mid F_j\right] \cdot P\left[F_j\right] \qquad (6.10)$$

where the event F_j denotes fatigue failure of component No.j and the event ($FSYS(U) \mid \mathrm{F_j}$) denotes system failure by overload given a fatigue failure. If no NDE is carried out, $P\left\lfloor F_j \right\rfloor$ will vary with time during the service life. If p_{FSYS} per year is to be determined, an average $P\left\lfloor F_j \right\rfloor$ per year in the service life should be determined or the maximum annual $P\left\lfloor F_j \right\rfloor$ per year should be calculated. Assuming that annual visual inspections are carried out to ensure that complete member/joint failure are detected after each winter season, $P\left\lfloor FSYS(U) \mid \mathrm{F_j} \right\rfloor$ could be determined as the annual probability of overload failure.

The implied p_{FSYS} for the two cases mentioned above, can then be estimated as follows:

<u>Case A:</u>

- Fatigue criterion: D = 0.33
- PLS criterion fulfilled

In this case the PLS criterion implies survival of an environmental load with exceedance probability 10^{-2} and load and resistance factors equal to 1.0.

The p_{FSYS} is estimated by Eq. (6.10), with $P\lfloor F_j \rfloor$ implied by the fatigue design criterion: D = 0.33 as discussed in Section 5. $P\lfloor FSYS(U) \,|\, \mathrm{F_j} \rfloor$ is determined as the annual probability of overload given that the structure with a failed member is designed to survive 100 year loads with load and resistance factors equal to 1.0.

The main question in estimating p_{FSYS} in this case is how many terms in the sum of Eq. (6.10) that should be assumed to contribute. One approach would be to consider a target level for each term in the sum, i.e. for each joint, separately.

<u>Case B:</u>

- Fatigue criterion: D = 0. 1
- No PLS or ALS requirement

In this case $P\lfloor F_j \rfloor$ is estimated as in the Case A, while $P\lfloor FSYS(U) \,|\, \mathrm{F_j} \rfloor$could be taken to be equal to 1.0. Also, in this case the question is how many terms contribute in Eq. (6.10).

By considering the target for each joint separately (by one term of Eq. (6.10) at a time), it may be that the p_{FSYS} accepted is several times the individual contribution. This would be the case if several joints have the same (maximum) contribution to the system failure probability. However, the approach suggested, is consistent with current practice which does not include a system safety factor which increases with the number of potential locations of initial fatigue failures (joints) that contribute equally to the sum in Eq. (6.10).

Obviously, using the maximum single contribution to the sum in Eq. (6.10) as the target level for systems reliability analysis in which all failure modes are considered, will be conservative.

Example

The approach outlined above can be illustrated by a simple example. The failure probabilities implied by fatigue design criteria are based on the example in Section 5.5.2. Hence, $P\lfloor F_j \rfloor$ for the service-life is approximately 10^{-2} and $6 \cdot 10^{-4}$ for D = 0.33 and D = 0.1, respectively. $P\lfloor FSYS(U) \,|\, \mathrm{F_j} \rfloor$for a system that fulfils the NPD PLS criterion can be approximately estimated from Eq. (6.7) with B_R = 1.08, μ_ψ = 1.0, $\gamma_R = \gamma_S$ = 1.0, H_{100}/H_1 = 1.3, α = 1.7, V_R = 0.15, V_ψ = 0.25, V_{H_1} = 0.15. This probability is $\Phi\left(-1.35\right) = 8.9 \cdot 10^{-2}$.

It is also noted that the probability of system failure given fatigue failure in Case B is set equal to 1.0, because there is no explicit criterion that ensures reserve strength in this case.

An estimate of the target probability of system failure in the service life is then

Case A: $p_{FSYS} \approx (8.9 \times 10^{-2}) \times (10^{-2}) \approx 8.9 \times 10^{-4}$

Case B: $p_{FSYS} \approx (1.0) \times (6 \times 10^{-4}) \approx 6 \times 10^{-4}$

These two cases, hence, imply similar target values.

6.6 COMMENTS ON IMPLIED TARGET RELIABILITIES

In Section 6 so far the failure probabilities implied by ULS (PLS) and FLS design criteria are used as basis to infer system target reliability levels. In view of Eq. (6.2) it is interesting to compare the relative contribution from to p_{FSYS} from the pure overload case and fatigue-induced system failure.

With the uncertainty measures indicated for ultimate strength above, the annual component failure probabilities may be of the order 10^{-3} to 10^{-4} and ultimate system failure probabilities would be 3×10^{-4} to 2×10^{-5}. The corresponding lifetime (20 year) values are 2×10^{-3} to 10^{-4}, while the lifetime probabilities of system failures induced by fatigue are of the order 5×10^{-4} in the examples in Section 6. This fact implies that the contribution to p_{FSYS} from pure overload and fatigue-induced modes may be of the same order of magnitude.

Obviously, the relative magnitudes of failure probabilities depend upon the assumptions about uncertainties measures that are made. While uncertainties relating to ULS conditions are common to the two types of modes (pure overload mode, overload induced by fatigue failure), the latter fatigue modes include unique uncertainties. A more detailed assessment of the uncertainties is required to make more firm conclusions about the relative contributions to p_{FSYS}.

6.7 OLD VERSUS NEW STRUCTURE

Up to now reference has been made to new designs. In theory the target level associated with individual fatalities and environmental damage should apply indifferently to old and new facilities. Only the cost-benefit analysis of given safety measures should be different and also reflect the remaining life of the facility. When using the cost-optimization approach in Section 3.4 it should be noted that the initial costs (b) of reducing the failure probability by a factor of 10 for all existing structure may be several times the costs (b) for a new structure. Eq. (3.4) accordingly implies a probability for existing structures, which is a factor α_{CB} times that for new ones.

In practice, however, even the individual risk criteria (both for workers on-site and for the public off-site) are typically relaxed for existing plants, essentially for economic reasons; upgrading old platforms to reach the basic required levels of individual safety would be too costly. The only alternatives may then be to close the facility, in which case the risk may simply be displaced. The energy (oil and gas) previously produced at the closed facility will be produced elsewhere but not without risks to others. One may also argue that closing a facility, if it increases unemployment, may create a higher risk to life for the fired workers than the occupational risk of working in the facility. Therefore, for a variety of economic and social reasons, there seems to be an agreement that it is appropriate to consider lowering the safety standards related to individual risks for older plants. See Paté-Cornell (1993) for a more detailed exposure.

Bea (1993) favours cost analysis to set target level for existing offshore structures. This approach implies a willingness to accept lower reliabilities for old systems compared to new ones. Iwan et at. (1993) proposed a target level based on separate considerations of the fatality rate and environmental damage, and arrive at a different target reliability level, however, also implying slightly lower target reliabilities for existing compared to new platforms.

API (1994) proposes a failure probability which is two times higher for existing than for new designs. The deterministic approach of API for reassessment of platforms is transformed into reliability terms by Krieger et al. (1994).

7. ASSESSMENT OF TARGET LEVELS GIVEN IN PIA

7.1 GENERAL

In this section the target levels given in Section 5 of PIA theory manual (1990) will be discussed on the general basis for establishing target levels outlined in Sections 1-6.

7.2 COMPONENT TARGET LEVELS

PIA refers to annual target levels for ultimate limit states for components implicit in NPD Regulations (1985) of the order 10^{-4} to 10^{-5}.

Moreover, PIA states that the target safety reliability indices implicit in NPD Regulations for primary members are 3.7 (splash zone), 2.6 (below water) and 2.0 (above water). It is noted that these numbers reflect the different levels of accessability for inspection. It is considered that it is better to establish target levels without allowing for NDE, only taking into account the possible minimum of annual visual inspection, which is carried out to monitor complete failure of members or joints. This is because the information about access of inspection used by NPD is vague. Nothing is, for instance, said about the frequency and quality of inspections and repair.

If the target levels are established without account of inspections, the effect of inspections can be accommodated in demonstrating compliance with the target level.

The PIA manual also gives (slightly) different target levels for SN - Miner Palmgren and fracture mechanics approaches. As mentioned in Section 5.3 it is important to calibrate the two approaches, both in a deterministic and probabilistic sense. Then, of course, it is only meaningful to apply the same target levels for the two approaches.

7.3 SYSTEM TARGET LEVELS

PIA adopts the simplest system model and considers the following failure modes

- overload failure of the platform as a whole
- fatigue-induced failure of individual members/joints and subsequent overload failure

The reliability model applied in PIA for the first failure mode is a simple log-normal (R, S) for the system as a single "component", with reference of R and S to the base shear. The probabilistic measures of uncertainty assumed are relevant except for the bias on environmental load. Also, the uncertainties applied in PIA deviate from those used in the example presented above.

PIA (1990) establishes a target level for the system's failure probability on the basis of

- system overload failure following a singe fatigue failure
- assumed fatigue (Miner) damage of 0.1 of the initially failed member, implying a life-time failure probability of 1.0×10^{-4} according to PIA
- reserve strength of the damaged system that ensures a conditional probability of lifetime failure of 1.0×10^{-2} (corresponding to a safety factor of the order of 1.5).

The target lifetime failure probability of the system is then inferred in PIA by a single term of Eq. (6.10) as:

$$p_{FSYS} = P\left[FSYS(U) \mid F_j\right] \cdot P\left[F_j\right] = (1.0 \times 10^{-2}) \cdot (1.0 \times 10^{-4}) = 10^{-6}$$

It is noted that the target value in PIA was established by associating a fatigue damage criterion of D = 0.1 for a damage tolerant structure (i.e. with $P[FSYS(U)|F_j] = 10^{-2}$. Another interpretation was made in Section 6.5, where D = 0.33 was considered acceptable if a PLS criterion with respect to failure of a given member/joint, was fulfilled.

Moreover, the fatigue failure probability (10^{-4}) is not consistently derived on the basis of the base case uncertainties applied in PIA. By applying the base case uncertainties of PIA, and the philosophy corresponding to Cases A and B in Section 6.5, the following values are obtained (Moan & al., 1999)

Case A

$$p_{FSYS} = P\left[FSYS(U) \mid F_J\right] \cdot P\left[F_j\right] = (0.11) \times \left(6.5 \cdot 10^{-3}\right) \approx 7 \cdot 10^{-4}$$

In the initial development of PIA it was conservatively chosen to require a system safety factor of 1.5. This implies a $P[FSYS(U)|F_j]$ of 9×10^{-3}, and hence a $p_{FSYS} = 6 \times 10^{-5}$

Case B

$$p_{FSYS} = (1.0) \times \left(7 \times 10^{-5}\right) = 7 \times 10^{-5}$$

The main reason for the discrepancy between the p_{FSYS} in the examples in Section 6.5 and in this section is the difference in uncertainty measures for the fatigue loading.

It is seen that the target levels implied by Cases A and B were quite similar if a system safety factor of 1.5 was considered for Case A. The present interpretation of NPD combined with the base case uncertainty measure of PIA imply that the NPD criteria and PIA base uncertainty measures are not consistent. Moreover, the implied target values seem to be larger than the target value of 10^{-6} applied in PIA. This fact implies that the PIA target values are conservative.

8. CONCLUDING REMARKS

The following general methods for estimating the target failure probability level have been outlined:

- the failure probability implicit in current codes
- risk of fatalities, environmental damage and property loss as inferred from world wide experiences and a postulate of the fraction of the risk that can be attributed to structural failure
- cost - benefit consideration (optimization)

The former method is advocated for component design cheeks of new structures to ensure consistency with existing design practice and the reliability methodology applied. However, other considerations are useful in setting target levels for structural risk analysis.

Target levels for components are proposed to be determined from relevant ULS and FLS design criteria.

The following methods to infer target levels for the probability of system failure based on existing design requirements, have been discussed:

- failure probability of components implied by ULS and FLS design criteria and a consequence modification factor to accommodate systems reliability target rather than a component level target
- failure probability implied by the NPD PLS criterion
- failure probability of components implied by NPD FLS requirements related to the systems safety

The target levels should be referred to annual or service-life values. This may be easily done for failure probabilities for overload failures for structures, which are subject to stationary load conditions and no deterioration of strength with time. Fatigue failures, however, result from cumulative effects and caution needs to be exercised in defining failure probabilities.

To be able to compare failure probabilities associated with both overload and fatigue failure modes, the probabilities should refer consistently to lifetime or annual values.

Special emphasis is placed on assessing target levels for a simplified systems approach (used e.g. in PIA) for fatigue. In this simplified approach the system failure probability is expressed as the probability of fatigue failure times the conditional probability of system failure given fatigue failure. It is suggested to calculate the system lifetime failure probability associated with fatigue-induced failure, by referring the fatigue failure to the lifetime and the subsequent overload failure to an annual value.

In this report the relevant reliability methodology is not defined in detail. Only simple methods are used to illustrate the procedures used to establish target levels. For this reason specific levels of target failure probabilistics are not given in the present report either.

Another issue is whether the target failure probabilities of existing structures should be different from those of new ones. Various socio-economic factors suggest that the target reliability level for existing structures may be reduced. Whether such factors should be accounted for depends upon failure consequences and the regulatory regime. For North Sea platforms the current regulatory attitude do not seem to be to allow reduced reliability level (increased target failure probability) for existing compared to new structures when the failure consequences may involve fatalities or significant pollution.

Finally, it is emphasised that the failure probabilities, which are compared to this target level, should be calculated by the same methodology as used to establish the target level.

Moreover, the uncertainty measures, reliability method and target level together with the relevant procedures for load and resistance assessment, should be consistent with acceptable design practice in the relevant regulatory regime. Since the applied methods vary, caution should be exercised in using general target values, i.e. without the justification of consistency as mentioned above.

If the target levels are determined in a consistent manner, the approach will be robust, i.e. not very sensitive to the assumptions made regarding uncertainties. The main concern in establishing and applying the target levels, is to balance the uncertainties applied for the ultimate global failure of the damaged system and those used in the fatigue analysis.

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