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18th INTERNATIONAL SHIP AND
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VOLUME 1

COMMITTEE III.2

FATIGUE AND FRACTURE

COMMITTEE MANDATE

Concern for crack initiation and growth under cyclic loading as well as unstable crack propagation and tearing in ship and offshore structures. Due attention shall be paid to practical application and statistical description of fracture control methods in design, fabrication and service. Consideration is to be given to the suitability and uncertainty of physical models.

COMMITTEE MEMBERS

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KEYWORDS

Fatigue, fracture mechanics, unstable crack propagation, multiaxinal fatigue, materials, rules and guidelines, damage control, inspection and life extension

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1 INTRODUCTION

Most of our knowledge on fatigue is either based on experience from structural or component failures, or from fatigue experiments. For more than 150 years, the fatigue and fracture engineering disciplines have been investigated but they are still developing and of large contemporary interest, not only for ship and offshore structures, but virtually for all engineering structures. Recent advances in computational mechanics together with the rapid increase in computer capacity have enabled the possibility to utilize more advanced, highly detailed fatigue and fracture analyses within the offshore and shipping industries. Consequently, today, there are several commercial tools available that are suitable for thorough structural integrity analysis of large-scale structures; see Figure 1 for an example of different state-of-the-art methods (accuracy versus complexity) for fatigue assessment of offshore and ship structures. However, despite the fast development of new commercial software, that can assist engineers in their daily work to design safer, lighter and more reliable structures, it is of outmost importance to continue striving for further improvement of existing fatigue design methods and development of new methodologies.

In the planning of the current report, the committee members decided to have new focus in the reporting on fatigue and fracture in contrast to previous reports. It was found of great value to pay attention to in particular unstable crack propagation, new materials, damage control and risk-based assessment, and update of latest changes in design methods for ships and offshore structures. Section 2 presents a brief overview of the recent developments in fatigue assessment methods. This was a significant part of the ISSC2009 report, and hence, it was decided to give it less attention in the current report; multiaxial fatigue analysis procedures is an exception here since a lot of work has been presented lately. Section 3 continues to present findings on unstable crack propagation. Methods for the analysis and experimental measurements of nucleation and propagation of brittle fracture are presented along with an example of how brittle crack propagation can be prevented in container ships.

Some advances in materials and structural details are presented in Section 4. Examples of investigations that present new findings on most commonly used materials today are presented, followed by extra high-strength steel, steel with improved crack growth properties, materials for cold climate, honeycomb structures, etc. In Section 5, methods suitable for damage control and risk-based assessment are reviewed, considering factors such as effect of workmanship, internal defects, welding procedure, etc. Attention is paid to the suitability and uncertainty of physical models, uncertainty in

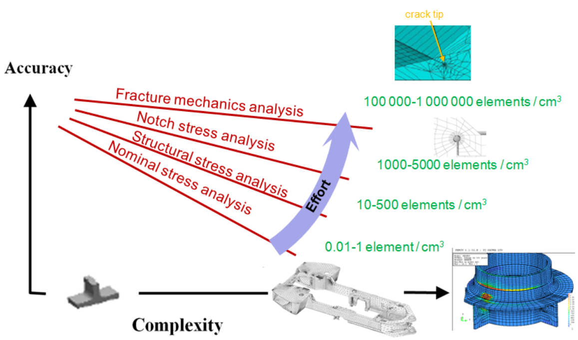

Figure 1: Illustration of different techniques that can be used to solve fatigue-related issues, Marquis (2009).

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fatigue assessment diagrams and fatigue life assessments, and finally, the analysis of ageing and aged ships and offshore structures is discussed.

Section 6 is a review of update and comparison of design methods for ships and offshore structures, such as the Common Structural Rules (CSR) for oil tankers and bulk carriers, the Harmonized Common Structural Rules (CSR-H) for oil tankers and bulk carriers, rules for offshore installations, Arctic design codes. Some of the procedures reviewed in Section 6 are challenged in a comparison presented as a benchmark study in Section 7. The intention of the benchmark study is to show how uncertainties in fatigue assessment of welded structures are treated when unconventional loading pattern is to be applied on a simple welded joint. Finally, in Section 8, recommendations for further research are presented followed by a summary of the conclusions in Section 9.

2 RECENT DEVELOPMENTS IN FATIGUE ASSESSMENT METHODS

Fatigue has become a critical limit state for engineers in designing a new structure or in assessing an existing structure. The next-generation offshore structures require an optimal fatigue design based on improved fatigue assessment methods supported by continuous research efforts. In contrast to the fracture mechanics, which are supported by substantial development in the theoretical and analytical solutions to engineering boundary value problems, the engineering assessment of fatigue failures has largely relied on formulations and procedures developed from empirical evidences. The development in the fatigue theories based on the classical continuum mechanics faces two critical challenges. Fatigue as an engineering phenomenon often involves material damage in a microscopic scale, which involves non-homogeneity of the material properties at these length scales. In addition, the fatigue damage entails continuous separation of the materials, which impinges on the fundamental assumption in the continuum mechanics. Despite the primitive theoretical development in the fatigue theory, engineering approaches developed from empirical database proves satisfactory assessments for realistic welded structural details. This chapter summarizes the developments in the fatigue assessment methods for the ship and offshore structural details over the last three to four years.

2.1 Low Cycle, High Cycle and Ultra-High Cycle Fatigue Approaches

Fatigue assessment methods separate into three broad categories based on the type of cyclic loading experienced by the structure, namely the strain-based low-cycle, stress-based high-cycle and ultra-high cycle fatigue. Low-cycle fatigue refers to the condition where fatigue failure occurs at a relatively low number of cycles (often less than 10^4 cycles) due to material damage incurred by macroscopic plastic deformations under cyclic actions, as illustrated in Figure 2. The high-cycle fatigue damage, on the other hand, occurs as an elastic phenomenon on a macro scale of the material, with the number of cycles ranging approximately from 10^4 to 10^7 cycles, with the latter often defining the fatigue endurance limit for structures under constant amplitude loading. This fatigue endurance limit does not exist for structures under variable amplitude loading. The fatigue behaviour beyond 10^7 is of particular significance for design against variable amplitude loading. The latest IIW design code (2009) includes a decay in the S-N curve for very high cycle applications beyond the fatigue limit.

Lotsberg (2010a) summarizes the latest improvement made in the revised DNV's design guide line for fatigue design of offshore steel structures, and highlights that the assessment of low-cycle fatigue has become a recommended practice in association

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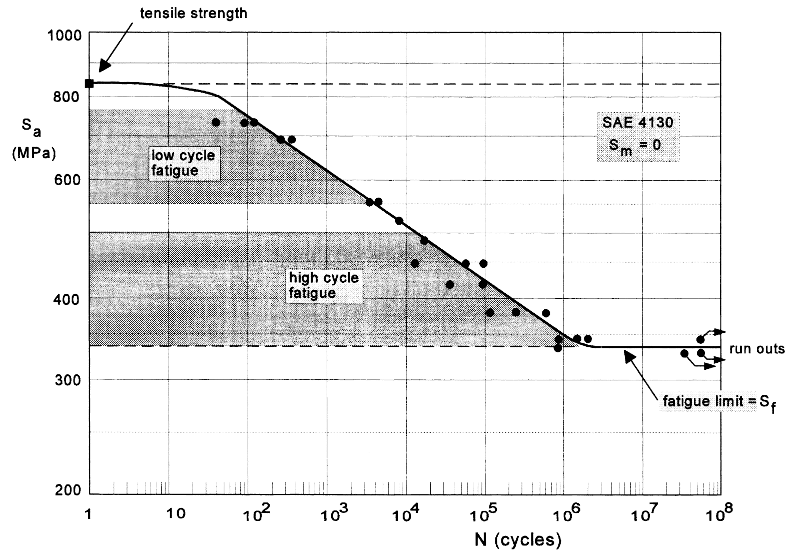

Figure 2: Low-cycle and high-cycle fatigue for structural details (Schijve, 2009).

with the assessment for an ultimate limit state, for example, for an offshore structure under a storm condition. He also points out that low-cycle fatigue is a more critical concern for details in ship structures than those in offshore structures, since the utilization ratios for ship structural details remain much higher than the utilization ratios in offshore structures under ultimate limit states.

Crupi *et al.* (2009, 2010) present a thermographic method to determine the entire S-N curve, covering high-cycle fatigue behaviour, the low-cycle fatigue behaviour and fatigue limit for welded details. The thermographic method operates on the temperature change caused by the applied cyclic stress. The maximum stress range that creates no temperature change becomes the theoretical fatigue limit for the structural material. The unique relationship between the stress range and the temperature change forms the basis to determine the S-N curve for the material. The thermographic method provides a rapid approach to determine the fatigue limit and the S-N curve based on very limited experimental data, and proves to be consistent with the conventional S-N approach in the IIW code. The behaviour of the structural components under very high cycle fatigue loading has recently become an important research topic. Marines-Garcia *et al.* (2007) report an important failure phenomenon for structural components loaded between 10^6 to 10^8 cycles. The location of the failure initiation may switch from the specimen surface to an interior "fish-eye". Liu *et al.* (2010) discuss the material effects on the probabilistic assessment of fatigue failure for high-strength steels under high-cycle fatigue or very high-cycle fatigue. They propose a modified Basquin's equation to predict the S-N curves for the high-strength steel specimens. Sosino (2007b) discusses the endurance limit of the S-N curves that most codes specify. He point out that in the high-cycle regime a decrease of fatigue strength with increased number of cycles is seen, hence assuming an endurance limit can be unconservative. He recommends that a decrease of the slope of the S-N curve for steels, cast irons and magnesium alloys should be 5 % per decade if no large tensile residual stresses are present.

Some different approaches for the high cycles area have been discussed by Rother

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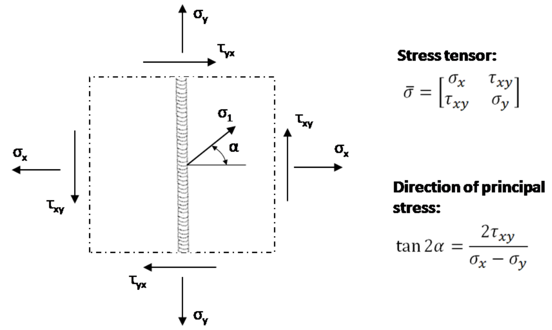

Figure 3: Multiaxial stress state on welded plate

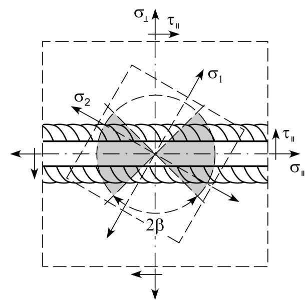

Figure 4: Biaxial oblique loading

and Rudolph (2011) who compared the hot-spot stress approach and the notch stress approach in the fatigue assessment of realistic welded structures, and recommended a hierarchical two-step approach for the critical structural components. The structural stress approach proves to be efficient in identifying the critical highly stressed locations for a subsequent detailed notch stress assessment. Erny *et al.* (2010) presented an approach to estimate the fatigue life of welded assemblies in ship structures, combining the finite element analysis results and the experimental investigation on the fatigue response of the material in the heat-affected zone. Radaj *et al.* (2009) reviewed the local approaches in the fatigue assessment of the welded connections. They concluded that all local concepts in predicting the fatigue life may possibly involve an unlimited number of variants in the modelling and estimation procedure. The local concepts do not separate the fatigue life into the crack initiation and the crack propagation. The crack initiation life may include an initiation life at the microstructural scale and a short-crack propagation life. Pujol and Pinto (2011) reported a novel approach based on the neural network method to predict the fatigue life. The neural network approach provides consistent predictions compared to other statistical models.

2.2 Multiaxial Fatigue

A multiaxial stress state refers to a loading condition where two or more stress components are acting on a critical plane of a structural detail, see Figure 3 and 4. This is especially important, when shear stress range is more than 15 % of the normal stress range or the direction of maximum principal stress α is changed significantly,e.g. more than 20°, during the cyclic loading, Hobbacher (2009). The individual stress components of multiaxial stress state should be considered. The stress components caused by complex loading may be occurring either in-phase or out-of-phase, i.e. be proportional or non-proportional. Non-proportionality means that the direction of the principal stress or strain is changing. The multiaxial fatigue behaviour is influenced both by the proportionality/non-proportionality as well as by the ductility of the material Sonsino (2009, 2011).

Failure mechanisms are controlled by shear stresses or strains in case of ductile materials, Sonsino (2009, 2011) and Wiebesiek *et al.* (2011). The normal stresses are dominating in case of brittle materials. For semi-ductile materials, failure mechanisms are controlled by the combination of shear and normal stresses. Consequently, the influence of multiaxial stress states differs according to the level of ductility. As shown in Figure 5, the change of the principal stress direction lowers the fatigue life of ductile materials noticeably. This effect does not occur in fatigue of semi-ductile materials

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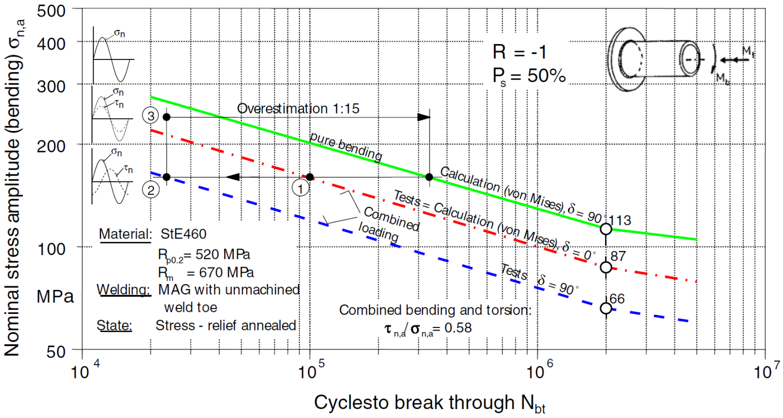

Figure 5: Fatigue strength of flange-tube welded connection under uniaxial and multiaxial loading. Number of cycles N_{bt} correspond to the crack depth of the tube thickness, Sonsino (2009).

and the change of principal stress direction may even increase the fatigue life of brittle materials. This is noticed for both constant amplitude and variable amplitude i.e. multiaxial spectrum loading. During the last decade, the problem of failure mechanisms in multiaxial fatigue has been investigated and several multiaxial fatigue criteria have been developed. Papuga (2011), Gómez *et al.* (2011) and Li *et al.* (2009) show overview and comparisons of different multiaxial fatigue criteria. The most common approaches are critical plane approaches, effective equivalent stress (integral plane) approaches and stress invariant based approaches.

A new effective stress method based on the resultant stress has been proposed by Kurinjivelan *et al.* (2010). This method can be used in conjunction with the normal stress S-N curve. The effectiveness of the method has been verified by comparing with the already existing methods for combined normal and shear stress cycles applied in phase. It is observed that by proposed method, the percentage of error is lesser when compared to the existing methods.

Some new critical plane approaches are trying to take the material properties also into account in addition to the stress state, Carpinteri *et al.* (2009). Reis *et al.* (2009) assess the applicability of the different critical plane criteria while considering the different cyclic plasticity behaviour, e.g. hardening effect, and sensitivity to non-proportionality of different materials. Susmel (2008, 2009, 2010 and 2011) suggest that the crack will initiate on the plane, which contains the direction along which the variance of the resolved shear stress reaches the maximum value.

Integral plane hypothesis, e.g. effective equivalent stress (EESH) or simpler modified Gough-Pollard for practical applications, are first mentioned by Sonsino (1995). For EESH local stresses or strains need to be known. The hypothesis states that the crack initiation is caused by the interaction of local shear stresses acting on different surface planes of the material considering the influence of non-proportional loading. These methods, unlike critical plane methods, are able to consider the phase difference in the loading. EESH is applied together with 0.05 mm fictitious weld root radius for laser beam welded thin steel structures under multiaxial in- and out-of-phase loading (Sonsino *et al.*, 2006) and together with 1 mm fictitious notch radius, Sonsino and Łagoda (2004). There, the EESH is compared with some of the other criteria and

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it seems to be the most suitable, giving the results on the safe side. However, the integral plane methods are even more time consuming than critical plane approaches.

Stress invariant based methods, see e.g. Vu *et al.* (2010), make use of the stress invariants, mostly the first invariant of the stress tensor and the second invariant of the stress deviator, and the maximum hydrostatic pressure. Stress invariant based methods aim at reducing the computation time. Recent papers discussing stress invariant based multiaxial fatigue criteria take the non-proportional loading into account. Both critical plane and integral stress or strain approaches use time-independent variables in the fatigue life assessment, while the idea behind Stress Space Curve Hypothesis (SSCH), presented by Wiebesiek *et al.* (2011), takes the complete time evolution of the very complicated multiaxial stress history into account. The method does not consider arbitrary stress signal, but rather the simplified shapes (ellipsoids). The EESH is a special case of SSCH. Castro *et al.* (2009) discuss the multiaxial fatigue criteria based on the equivalent shear stress amplitudes. They applied the maximum scalar measure of the different hypersurfaces enclosing the deviatoric stress history. Using the same method, Mamiya *et al.* (2011) have developed a multiaxial fatigue life estimation procedure based on a piecewise ruled S-N surface. Cristofori *et al.* (2008) discuss stress invariant based method which decouples the loading path by projecting it along the directions which are defined by the loading path itself. Brighenti and Carpinteri (2011) propose a method for fatigue assessment of structural components under complex multiaxial loading, which uses the damage accumulation expression with an appropriate endurance function containing stress invariants and the deviatoric stress invariants. The method does not require the evaluation of the critical plane nor any conventional loading cycle counting algorithm.

Although in the last two decades significant progress in the knowledge and theoretical modelling has been achieved, a generally applicable multiaxial fatigue criterion does not exist. No solid proof has been obtained to confirm the validity of the hypotheses for the constant amplitude loading in case of variable amplitude loading. Sonsino (2009) uses relatively low allowable stresses so as to cover most of the experimentally obtained results under multiaxial loading. However, this approach does not consider the physics behind the complex multiaxial fatigue damage behaviour. Recommendation of IIW, Hobbacher (2009) includes the modified Gough-Pollard algorithm for both constant amplitude and variable amplitude loading. The IIW recommendations suggest Palmgren-Miner damage sum $D = 0.5$ for steel under variable amplitude loading, when the load spectrum is narrow banded, i.e. the mean stresses are constant. If the load spectrum is wide banded, IIW recommendations suggest damage sum $D = 0.2$ according to Sonsino (2007a) to capture the mean stress effect. These damage sum values include the positive influence of the variable amplitude loading on the fatigue life and thus they are slightly higher than the real damage sums. For further information regarding multiaxial fatigue see Section 7.

2.3 Factors Influencing Fatigue

The fatigue damage and degradation of a material is affected by a large number of factors such as mean stresses and their redistribution, residual stresses, loading of the structure including load sequences, structural dimensions and plate thickness, corrosive environments and temperature of the surroundings, the design criterion, fabrication technology and methods for improving fatigue performance, and sensitivity of the material. This section focuses on recent developments with this topic and should be read in conjunction with the ISSC 2009 – Fatigue and Fractures committee report, where the topics have been described comprehensively.

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2.3.1 Thickness and size effect

A commonly used approach to estimate the reduction in fatigue strength of welded structures is to multiply the stress range by a factor $k_s = (t_{ref}/t)^k$ where t is the relevant plate thickness, t_{ref} is a reference thickness and k is an empirical constant, Hobbacher (2009). Kim *et al.* (2009) report the fatigue tests on butt-welded steel plates with thicknesses varying from $20\,mm$ to $80\,mm$ under three different applied stress ranges. The specimens demonstrate a decreasing fatigue life as the thickness increased. Most experimental work has been devoted to joints with plate thicknesses typically larger than $25\,mm$. In Gustafsson (2006), experimental data from constant amplitude fatigue testing of non-load carrying welded joints in high strength steel of thickness $3 - 12\,mm$ are presented. The results show an increase in fatigue strength with decreasing sheet thickness down to $3\,mm$. Similar findings were obtained by Ringsberg *et al.* (2008) in their fatigue testing of nuts welded to thin sheets

2.3.2 Corrosive environment and temperature

Many experimental investigations have contributed to significant findings in the area of corrosion fatigue for offshore applications. A few research groups; Eslami *et al.* (2010) and Ishikawa *et al.* (2008) investigate the stress corrosion crack initiation of pipeline steels in a near-neutral pH environment. Schroeder and Müller (2009) report the fatigue and corrosion fatigue behaviour of a typical alloy used in the riser construction of offshore platforms. Pargeter *et al.* (2008) report the corrosion fatigue investigation on steel catenary risers in sweet production. Vennemann *et al.* (2008) present the bending fatigue tests of large diameter steel wire rope for subsea deployment. Yang *et al.* (2009) report the fracture toughness testing of SE(T) specimens in a sour environment to simulate the corrosion on offshore pipelines and risers. Holtam *et al.* (2009) report an investigation into fatigue crack growth test methods in a sour environment. Thierry *et al.* (2010) report a series of experimental investigation on steel materials used in the offshore platforms tested under very high cycle regime with three different ambient environments: 1) laboratory air, 2) laboratory air after pre-corrosion and 3) real-time artificial sea-water flow. The fatigue strength of the specimens tested in real-time sea-water environment at 10^8 cycles decreases significantly by a factor of 74 % compared to the specimens tested in the laboratory air and 71 % compared to the pre-corroded specimens. These results can be compared to "an offshore design practice" where S-N curves for subsea installed items in free corrosion are reduced by a factor 3 relative to S-N curves in air.

2.3.3 CA and VA loading, residual and mean stress effects

In service, the great majority of marine structures are subjected to variable amplitude loading while the design of the structures generally is based on fatigue data for constant amplitude loading combined with e.g. a Miner's linear cumulative damage model and an adequate safety factor. The validity of the above approach was investigated by Zhang and Maddox (2009) conducting experiments for Variable Amplitude (VA) loading based on two types of welded specimens. In some cases, VA loading is significantly more damaging than Constant Amplitude (CA) loading. The experiment indicates that the influence of mean stress is less significant compared to possible sequence effects. In addition, they documented that some shakedown can occur during the life time of the structure due to external loading and the above approach might therefore be too conservative. Based on tests including part of a full-scale driven foundation pile, which formed part of the support for the Edda Tripod for 30 years, Lotsberg *et al.* (2010b) show that only minor shakedown of residual stresses occurred

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during installation and service. It was therefore recommended still to use S-N curves allowing for residual stresses up to the yield stress.

2.4 Fatigue Crack Initiation

The fatigue damage process and crack initiation in steel material starts with dislocation movements forming slip bands, which nucleate, causing micro cracks inside grains in the microstructure. When the density of micro cracks is high enough, they coalesce together causing a short crack, which grows under cyclic loading. This crack initiation process is significantly affected by the material microstructure since the crack forming is controlled by the largest grain size, McDowell and Dunne (2010). In addition, the microstructure barriers i.e. grain boundaries affect the short crack growth until the crack size is more than 8 times averaged grain size Kawagoishi *et al.* (2000). Lautrou *et al.* (2009) proposed an approach to estimate the fatigue crack initiation life for welded steel joint by implementing a two-scale damage model in the finite element analysis. When the the geometry of the crack starts to control the crack growth rate, a long crack is initiated. The theoretical models for fatigue crack initiation can be divided into two groups: microstructure-sensitive mesoscale models and continuum based models.

The mesoscale models are based on the crystal plasticity, and thus they can capture the main feature of the microstructure; the size, direction and location of the individual grain. Roters *et al.* (2011) and McDowell and Dunne (2010) provide a good overview about the mesoscale modelling and the microstructure-sensitive computational methods for the fatigue crack forming. The mesoscale model is applied by Guilhem *et al.* (2010) investigating the effect of the grain clusters on fatigue crack initiation, while Romanova *et al.* (2011) investigated the influence of the grain boundaries on the plastic strains in different weld zones under static loading. Cyclic plasticity under variable loading has been tackled by Li *et al.* (2011). Luo and Chattopadhyay (2011) introduce multiscale damage criteria for the crack forming, and Owolabi *et al.* (2010, 2011) apply a probabilistic mesoscale model to the damage process zone and notch effect. All these studies aim at increasing the physical understanding about the crack forming and the influence of the different property of the microstructure. However, the special challenges still exist to model the short crack growth between grains, and to define reliable material parameters required for the quantitative fatigue analysis.

The continuum based approaches, e.g. the strain-based approach, are the most traditional way to model the crack initiation. These approaches include the effect of microstructure implicitly through the cyclic stress-strain curve and the fatigue strength coefficient i.e. Coffin-Manson equation, see e.g. Radaj *et al.* (2009). There the notch effect is typically captured using Neuber's rule. The strain-based approach is successfully applied by Lassen and Recho (2009) to derive more accurate physically based S-N curve for welded steel joints. Pakandam and Varvani-Farahani (2011) study the applicability of different strain-based energy approaches for welded joints, and they conclude that the critical plane based approach gave the best agreement with the fatigue test results of different welded joints. However, as noted by Beretta *et al.* (2009), the strain-based approach can fail particularly for sharp notch and variable amplitude loading. The main limitation of the strain-based approach is that the analysis is based on the initial geometrical shape of the weld notch, and thus, the effect of crack growth on the stress-strain state and gradient is not considered. The fracture mechanics corrects this weakness, but it requires the initial crack. Fracture mechanics based approach to marine structures has been developed by Cui *et al.* (2011). Alternative interesting approach has been presented by Mikheevskiy *et al.* (2009, 2011)

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and Wang *et al.* (2011), when they applied the strain-based approach to determine the fracture mechanics parameters i.e. fatigue crack growth rate. However, the further investigation of the limits of the fracture mechanics based approaches is required, since they neglect the period of the crack forming and micro crack coalesce to the short crack. In general, the application of the different continuum-based approach for practise is also challenged the lack of the material data. Although the material data is available for different parent material, see Basan *et al.* (2011) and Wang and Cui (2009), the material data for welded joints is very limited.

2.5 Fracture Mechanics Approach (Propagation Phase; Toe and Root Cracking)

The S-N approach often covers the total fatigue life of a structural component's fatigue life from the crack initiation to a through-propagation of the fatigue crack. Coupled with a Paris-type law to determine the fatigue life consumed during the crack propagation, the S-N approach then enables an approximate estimation on the fatigue life corresponding to the fatigue crack initiation. The empirical Paris Law has become a widely recognized approach to estimate the propagation life of a fatigue-induced crack in ship and offshore structures. The Paris Law dictates that the rate of the fatigue crack growth depends on the range of the stress-intensity factors, which uniquely determines the stress-strain fields near the crack tip for a small-scale yielding condition. The practical application of Paris law requires an assumption on the initial fatigue crack size, since the fatigue driving force for tiny cracks often fall below the Paris' threshold.

2.5.1 Crack Growth Rate Models

The Paris Law, when applied to estimate the crack propagation life for components or structures, often shows dependence not only on the loading range, but also on the maximum or minimum load level. Noroozi *et al.* (2007) propose a two-parameter fatigue crack growth driving force to include the effect of the load ratios. Recent research efforts lead to the fatigue crack growth models based on the energy principles, Bian and Taheri (2008), which include the effect of elastic-plastic deformation near the crack tip and the mixed mode loading, Liu and Mahadevan (2007). Shahani *et al.* (2009) compare the fatigue crack growth rate model expressed in the cyclic range of stress-intensity factors (ΔK), the range of crack mouth opening displacement ($\Delta CMOD$), the range of crack-tip opening displacement ($\Delta CTOD$) and the range of energy release rate (ΔJ). The comparison with the experimental data reveals that the crack growth rate expressed in terms of $\Delta CTOD$ and $\Delta J$is independent of the loading ratio, while the crack propagation rate expressed in ΔK and $\Delta CMOD$ exhibits strong dependence on the loading ratio. To illuminate the need for the analytically intractable stress-intensity factor solutions for most 3-D structural geometries, Pugno *et al.* (2006) propose a generalized Paris law to estimate the fatigue crack growth. This generalized Paris law replaces the stress intensity factor range by the stress range,

$$da/dN = B\Delta\sigma^n a^m \tag{1}$$

where B, n and m are material constants. The generalized fatigue crack propagation rule removes the requirement to compute the stress-intensity factors for complicated geometries and different crack sizes. Li *et al.* (2008) propose an improved, normalized Paris law to estimate the fatigue crack propagation,

$$db^*/dN = Cf^m\left(b^*\right)\Delta\sigma^m a^{m/2-1} \tag{2}$$

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where $f(b^*)$ refers to the normalized stress-intensity factor range. This improved model predicts closer agreement for the propagation of surface cracks in rectangular plates.

2.5.2 *Fatigue Crack Growth Assessment*

The crack propagation in structural materials imposes strong effects on the fatigue life assessment of offshore structures. The effect of crack initiation and propagation at multiple crack sites has attracted some research efforts in recent years, since realistic fatigue cracks often initiate from multiple locations around the hot-spot region as observed in many experimental research. Mkaddem and El Mansori (2010) proposed an equivalent ellipse method to analyse the fatigue behaviour at multi-surface initiations. Bozic *et al.* (2010) investigated the growth of multiple fatigue cracks in plates under cyclic tension. Their experimental investigations revealed that three collinear fatigue cracks in a plate lead to higher crack growth and a 40 % shorter fatigue life than a single crack in a plate.

The estimation of the crack growth life under fatigue loading predicates essentially by the Paris law, which has been widely applied to the assessment of many structural components in ship and offshore structures, Niu *et al.* (2009) and Larrainzar *et al.* (2010). Saxena *et al.* (2009) showed an examples of the fatigue life prediction for surface cracked straight pipes. They concluded that the Paris law provides a reasonable estimation of the fatigue propagation life compared to the experimental data. Their study also demonstrated that the SIF computed from semi-elliptical surface cracks should not be used to assess the fatigue life if the pipe has a constant depth crack. Hachi *et al.* (2010) proposed a hybrid weight-function approach to predict the fatigue crack growth for elliptical cracks in welded joint structure. Their approach assumes that the elliptical crack evolves into a circular crack under fatigue loading. Nykanen *et al.* (2009) developed a simplified fatigue assessment method for high quality welded cruciform joints. Their approach utilizes the linear-elastic fracture mechanics theory to develop an equation between the geometric parameters and the fatigue strength of the welded joint. They compared their method with 152 experimental fatigue data points.

Darcis *et al.* (2009) report the experimental investigation of fatigue crack growth rates in pipeline steels. Combined with the compliance method obtained from the FEA approach, the Paris law leads to accurate predictions of fatigue crack growth data and true crack lengths. Herrera *et al.* (2010) report the experimental study in seam welded API 5L X42 pipeline steels with fatigue crack growth in three separate zones, where the base metal shows the strongest resistance against fatigue crack growth among the three materials, while the weld metal exhibits the least resistance. The fatigue crack growth in the weld metal and in the heat-affected zone follows closely the prediction of the Paris's law while the fatigue crack growth in the base metal demonstrates more scatter.

Feltz *et al.* (2010) and Fischer *et al.* (2011) worked on a reliable and practical fatigue assessment of the partial penetration welds, which can be found in many ship structures. Depending on the throat thickness of the weld, the crack can initiate at either the toe or the root of the weld. The preferable approach should be able to distinguish between these two modes. The work carried out by the authors includes experiments, mixed mode crack propagation analyses using FRANC2D, results from notch stress approach and results from a newly developed Notch Stress Intensity Factor (N-SIF) approach. All three approaches were found to provide relatively reliable

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prediction of failure mode, but the estimate fatigue lives were found to be conservative. Fricke (2009) addresses the notch stress approach in the IIW guideline for the Fatigue Assessment by Notch Stress Analysis for Welded Structures which reviews different proposals for reference radii together with associated S-N curves.

Zacke *et al.* (2010) investigated the safety against fracture of block joints welded with weaving and string-bead techniques with large gaps in shipbuilding by use of fracture mechanics evaluation based on the FITNET procedure. They carried out CTOD testing of the two techniques, and they concluded that it was beneficial for the fracture behaviour of the weld to have wider welds (e.g. 30 mm) with sufficient strength properties for the welds carried out by the weaving techniques; this was not seen for the string-bead technique where also critical crack lengths were much smaller than for the weaving technique.

In addition, there have been some new developments in estimating the fatigue damage of fatigue cracks in recent years. Lassen and Recho (2009) proposed a more accurate nonlinear S-N curve to estimate the fatigue life for welded steel joints. The proposed method separates the fatigue life into a two-phase procedure: the crack initiation phase and the crack propagation phase. The crack initiation life depends on the local notch stress while the crack propagation life derives from the integration of the Paris law. In parallel, Makkonen (2009) also presented a new method to estimate the total fatigue life of a structural component, consisting of the estimation of the crack initiation life and that for the crack growth life. The estimation of the crack initiation life utilizes a statistical approach while the estimation of the crack growth life employs the fracture mechanics based Paris law. Rozumek (2010) compares three empirical formulae based on the J-integral values to estimate the fatigue crack growth rate in the stage II fatigue crack growth. The three formulae include the fundamental Paris equation expressed in ΔJ; the Paris equation expressed in ΔJ and including the stress ratio effect and the crack growth model including the stress ratio and the fracture toughness of the material. The empirical formula including the both stress ratio and the fracture toughness demonstrate the best agreement among the three formulae.

2.5.3 Overload Retardation

Overloading incurred in a cyclic fatigue loading event creates a large compressive residual stress near the crack tip, causing the fatigue crack growth rate to decrease. Numerous experimental evidences; Bichler and Pippan (2007), Le Roux *et al.* (2009), Duan *et al.* (2008), Jacobsson *et al.* (2010) and Chen *et al.* (2008) reveal that overloading condition can cause significant retardation in fatigue crack growth rate of standard laboratory-scale compact tension specimens. Codrington (2009) investigate the effect of plate thickness on the retardation of fatigue crack growth caused by an overload. The effect of retardation caused by the overload generally decreases with plate thickness, due to the reduced plastic residual stress field in the thick, highly constrained crack fronts. Harmain (2010) propose a model to include the effect of overload retardation on the fatigue crack growth by a retardation factor, F_R;

$$\frac{da}{dN} = CF_R(K_{max} - K_{op})^m \tag{3}$$

where K_{op} refers to the stress intensity factor at the closure of the crack surface, K_{max} denotes the maximum stress intensity factor in cyclic load, C and m are material constants. The retardation factor depends on the size of the plastic zone caused by the overload in relation to the plastic zone caused by the constant amplitude loading. The

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retardation in fatigue crack growth due to plasticity-induced crack closure has been examined by Osawa and Sumi (2008) on ship structural details through a numerical investigation. They concluded that the load interaction effects slow down significantly the fatigue crack growth rate and the prediction rendered by the conventional Paris law provides a conservative estimation of the fatigue life.

2.5.4 Fracture Toughness and Stress Triaxiality

The final stage of the fatigue crack propagation often involves rapid and unstable crack growth, which leads to a complete separation of the material over the remaining ligament. Al-Mukhtar *et al.* (2010) demonstrate in their estimation of the fatigue crack propagation life that the crack size at which unstable crack propagation takes place does not impose a significant effect on the fatigue life estimation for different plate-type welded joints. Their calculation, therefore, assumes the crack size corresponding to the unstable crack propagation as half of the plate thickness, for weld toe cracks with the crack propagation path perpendicular to the applied load. Troshchenko (2009) investigated the fracture resistance of metals at the end of cyclic fatigue loads and concluded that for fatigue crack-tips under plane strain conditions, the material exhibits much lower fracture toughness values at the final fracture at the end of the fatigue load cycles than the material fracture toughness measured in fracture specimens under predominantly static loading.

The stress triaxiality affects ductile failure even in a microscopic level. Ohata *et al.* (2010) developed a meso-scale 3D simulation model to predict the effect of microstructural morphology in ferrite-pearlite steel on ductile failure resistance, incorporating the damage mechanism to control ductile cracking by strain localization between different microstructural phases. Ductile crack nucleation and subsequent growth and linking were simulated by damage evolution model including plastic potential to nucleate micro-void. Effect of applied triaxial stress state on critical macro-strain for ductile cracking was predicted, Figure 6.

Regarding unstable ductile crack propagation and arrest in high-pressure gas pipelines, the Battelle two curve (BTC) method has been widely used. The method compares gas decompression curve and crack driving force curve, both of which are expressed in terms of pressure, and predicts whether an axial crack propagates unstably or not. Japanese HLP approach also compares the two curves but can simulate a history of crack propagation and arrest. It has long been pointed out that the BTC needs correction for high-pressure, high-strength pipelines. A new model is proposed, which takes account of the interaction between gas decompression and crack propagation, Misawa *et al.* (2010). More accurate evaluation of the gas decompression and crack

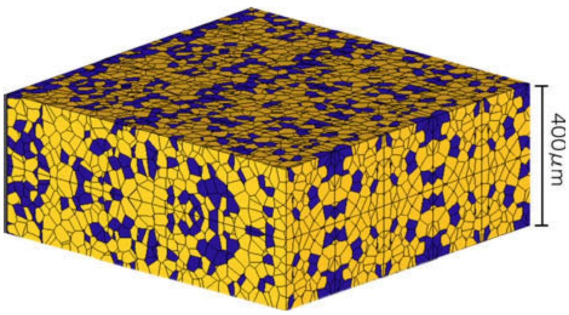

Figure 6: Meso-scale 3D numerical simulation of ductile fracture, Ohata *et al.* (2010).

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resistance curves are of crucial importance for better prediction of the unstable ductile fracture, Botros *et al.* (2010) and Duan *et al.* (2008).

2.5.5 Fracture Mechanics for Strain > 0.5 %

There is a general agreement in the industry that stresses above 0.5 % strain constitute strain based design. Currently, pipelines are designed for strains above 0.5 % due to reeling or environmental loads such as e.g. grounding, ice scouring and gouging, seismic loading and bottom snaking. Most research within this topic looks at the effect of internal pressure, the strain hardening characteristics of the pipe body material, degree of weld strength overmatch and the location of flaws. The topic was discussed in detail by the previous member of the ISSC fatigue and fracture committee, and hence the following section will focus on the latest research within this topic.

Extensive studies have been made for the strain-based design of linepipes, including full-scale tests; Mannucci *et al.* (2011), Tajika *et al.* (2011), Chen and Ji (2011), numerical simulations by Sandvik *et al.* (2011) and design methodology by Bjerke *et al.* (2011). Fairchild *et al.* (2011) provided a methodology of strain-based engineering critical assessment (SBECA) for ductile fracture. The methodology is based on the comparison of the tangency of the ductile tearing resistance curve (R-curve) and crack driving force and can be used to determine tolerable girth weld flaw sizes. A plot of about 150 weld metal and HAZ SENT R-curves was used to select three characteristic R-curves, Figure 7. The methodology has been calibrated and validated using about 50 full-scale tests covering a wide range of pipe geometries, material properties, degrees of girth weld misalignment, girth welding methods, and degrees of internal pipe pressure.

Stephens *et al.* (2009) present results of a large scale strain-based testing program of welded tubes where a range of parameters known to have significant impact on axial strain capacity of girth welds were examined, see Figure 8. A challenge is to estimate the strain from the deformation in full scale pipes and curved wide plate tests and they showed that axial strain could differ by more than 30 % depending on the location of the strain gauges. Hence, they recommend using a gauge length that bridges the girth weld and flaw location and also CMOD (crack mouth opening displacements) is contracted from the total elongation in order to be conservative. Wang *et al.* (2009) study the correlation between small-scale specimen material properties with large scale

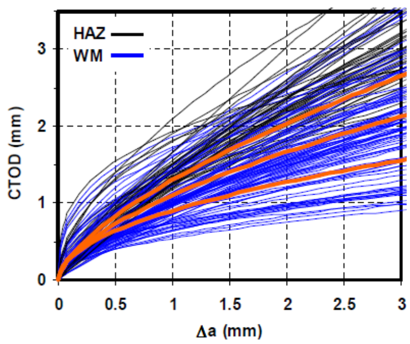

Figure 7: Ductile crack resistance curves of pipe girth weld metals for engineering critical assessment methodology, Fairchild *et al.* (2011).

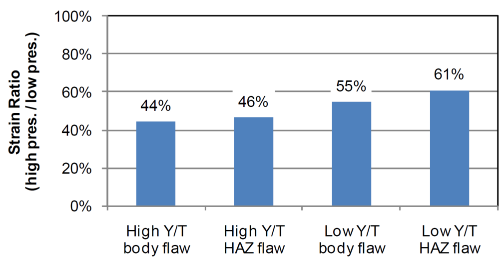

Figure 8: Effect of pressure on strain capacity (all flaws 3 x 50 mm with HAZ flaws adjacent to over-matched welds) Stephens *et al.* (2009)

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experimental test results including curved wide plate and full-scale pipes. Results presented showed uncertainties in the strain level in the range of 25 % when comparing FEM and full scale data. They conclude that understanding the true material response is important for all cases involving strain based design, and further work is still needed.

3 UNSTABLE CRACK PROPAGATION

Unstable crack propagation is of a type of fracture event in which a crack propagates at high velocity and long distance without increasing applied load, displacement or pressure. There are two types of unstable crack propagation; brittle and ductile crack propagation. Microscopic mechanism of brittle crack propagation in steel is predominantly cleavage fracture at high speed. The crack velocity of unstable ductile crack propagation is lower than that of brittle fracture, because energy consumption of this type of fracture is much larger than that of brittle fracture, large amount of stored energy is necessary for driving the crack. Prevention of unstable crack propagation is crucially important because it can lead to sudden catastrophic failure of the structures. Brittle crack propagation accidents can be prevented by applying steel plates with increased crack propagation resistance, or crack arrestability of base metal, together with proper crack arrest design. The crack arrestability of steel plates has been improved by applying e.g. thermo-mechanical control process (TMCP), see Section 4.

Unstable crack propagation has gained increased attention as design of offshore structures moves into artic regions where low design temperatures increase risk of brittle fracture. Furthermore, recent trend of adopting very thick steel plates in the design of large containerships and offshore structures has raised a concern regarding the reconsideration into brittle crack propagation because the brittle crack is more liable to propagate in heavier section steels due to the constrain effect (see Section 3.1.2). Recent developments of the research in this field are extensive, like standardization of crack arrest toughness testing and crack arrest design.

3.1 Nucleation of Brittle Fracture

The term used to describe a materials failure resistance to brittle fracture is fracture toughness; low toughness indicates a low resistance to brittle fracture and it is a functional of material properties such as microstructure and yield strength but also strongly dependent on the temperature, see Figure 9 where an upper and a lower shelf for toughness is defined. Fracture toughness is measured using highly constrained specimens, as represented by SENB specimens.

The Crack-Tip Opening Displacement has been widely used as fracture toughness parameters. The recent ASTM E 1290-08 employs a CTOD evaluation formula based on J-integral, which is different from the conventional formula based on the plastic hinge deformation model adopted in BS 7448. Tagawa *et al.* (2010) found that ASTM E 1290-08 based CTOD tends to give a smaller value than that of BS 7448 based CTOD. Ratio of the CTODs was about 60 % for low yield-to-tensile ratio steels. From direct measurement of CTOD of the SENB specimens, they found that ASTM E 1290-08 based CTOD corresponded to the CTOD of the thickness average, whereas BS 7448 based CTOD gave the CTOD at mid-thickness position. Considering the fact that most of the cleavage fractures are initiated at or near the mid-thickness positions, they presumed that the conventional BS 7448 based CTOD is more preferable for describing cleavage fracture initiation toughness, Figure 10.

A case study of the low temperature CTOD toughness for welded joints of the module stool of an FPSO is presented by Miao *et al.* (2010). Based on the international

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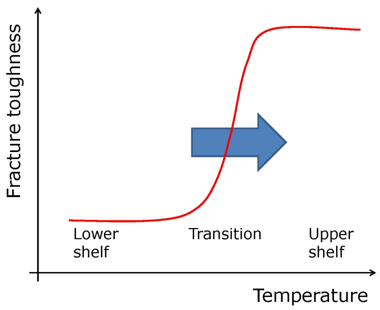

Figure 9: Fracture toughness as function of temperature

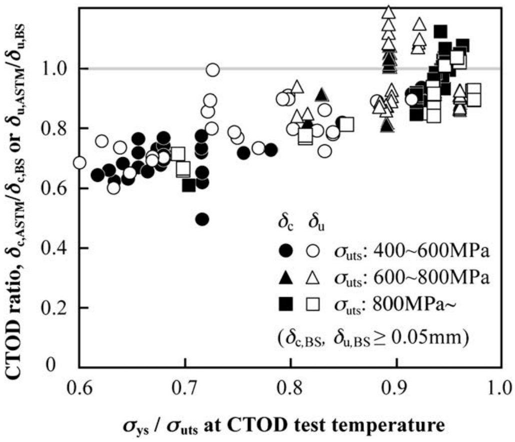

Figure 10: Comparison of E1290-08 and BS 7448 CTOD evaluation formula, Tagawa *et al.* (2009).

standard BS 7448 and DNV-OS-C401, a low temperature (-18° C) crack tip opening displacement (CTOD) test was conducted in the weld centre and the fusion line of the module stool. The results show that the CTOD values meet the requirements of DNV-OS-C401 (not less than 0.15 mm). Therefore, the preliminary welding procedure specification test can be used to weld the module stool for *et al.* which no further heat treatment is needed. Validation test like full scale and curved wide plate test (CWPT) is most often carried out at room temperature, however the integrity of the pipe needs to be proven for the lowest anticipated service temperature (LAST). Pari *et al.* (2010) investigate the use of Charpy V-notch testing in order to ensure upper-shelf fracture toughness. The goal was to provide guidance as to what CVN requirements are needed to ensure an upper-shelf performance, see Figure 10, and several models were investigated. They concluded that further work was needed, like the thickness effect, develop an upper-shelf temperature shift relation, finally the correlation to CTOD and SENT testing would be useful.

3.1.1 Influence of Constraint Effects

Fracture toughness is strongly dependent of the geometry and loading conditions. The standardized fracture toughness testing procedures such as those provided in ASTM and British Standards are intended to define a lower bound toughness value for the material or weld in question. These conventional toughness specimens like e.g. Compact Tension (CT) and Single Edge Notch Bend (SENB) have high constraint at the crack-tip and will provide lower bound fracture toughness with respect to the local geometry. Size criteria can be applied to prove that the test result is representative (conservative) when testing of these small scale test specimens are applied to large structures. Constraint corrections can be carried out either by correcting a higher constraint specimen or by using specimen with more similar constraint level found in the component in question. For both approaches there is a need to estimate the constraint level in the structure.

The constraint effects can be discussed based on the Weibull stress analysed, where determination of Weibull shape parameter, m, is an important issue. Handa *et al.* (2008) examined the values of m for a number of structural steels. No clear correlation could be observed between m and other parameters, including tensile properties, microstruc-

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tures, test temperatures, test specimen configurations and other parameters. However, they observed a mild dependence of lower bound value of m on critical CTOD, δ_{cr}; roughly 10 for δ_{cr} smaller than 0.05 mm and 20 for δ_{cr} larger than 0.05 mm. Tagawa *et al.* (2010) evaluated the scatter in fracture toughness in the ductile-brittle transition temperature region for 500 MPa class low carbon steel. They found that at -60° C the toughness showed a single modal Weibull distribution with $m = 4$, while in the temperature range from -20° C to -10° C, the distribution had a bilinear distribution with elbow points, beyond which the value of m did approach 2. The cause of bilinear toughness distribution was discussed taking account of subcritical crack growth, similar tendency was also seen by Wsposito *et al.* (2007). See also Østby *et al.* (2011) who investigated the fracture toughness scatter and the effect of constraint in weld thermal simulated HAZ microstructures at -60° C.

3.1.2 Cleavage and Brittle Bracture Nucleation in Welded Components

Welded joints are liable to brittle fracture initiation due to many reasons. One of the most influential factors is welding residual stress. Although the influence of the welding residual stress on brittle fracture initiation is well conceived and most of the fitness-for-service methods take its account, quantitative analyses have not yet been well undertaken. Yamashita *et al.* (2010) conducted a series of experiments using 780 MPa class high-strength steel welds and numerical analyses based on the Weibull stress criterion. They found that critical CTOD of wide plates with residual stress can be simulated using the critical Weibull stress distribution which was determined by deep-cracked three-point bend specimens by considering the effects of both the increase in the crack-tip stress due to welding residual stress and the stress decrease due to plastic constraint loss in the wide plates. They further applied the concept of equivalent CTOD ratio, β, under the welding residual stress field and conducted fracture assessments for "After Weld Notch" and "Before Weld Notch" type welded joints within the framework of failure assessment diagram (FAD). A FAD diagram both evaluates failure due to brittle fracture or plastic collapse. They concluded that an excessive conservatism observed in the conventional procedure is reasonably reduced by applying the proposed methodology, Figure 12. Minami *et al.* (2010) investigated the effect of weld metal overmatch (too high yield stress of weld material relative to base material) on stress field and constraint in HAZ. They concluded that the constraint was increased with increasing overmatch ratio. Nevertheless, the strength mismatch effect was marginal under large scale yielding conditions, except for extreme overmatch conditions, Figure 11. Strength matching between weld metal and base metal

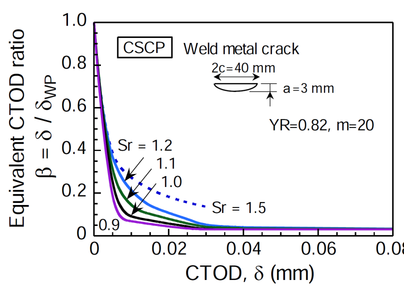

Figure 11: Influence of weld metal overmatch on equivalent CTOD ratio, Minami et al. (2010).

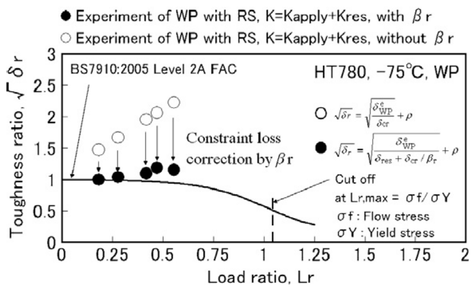

Figure 12: Prediction of brittle fracture initiation by FAD taking residual stress and constraint loss into account, Yamashita *et al.* (2010).

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is also an important factor influencing brittle fracture initiation. Ishikawa *et al.* (2007) previously showed that high hardness of weld metal resulted in lower fracture initiation toughness than expected from Charpy impact test properties at the weld bond in heavy thickness high-strength shipbuilding steel. Minami *et al.* (2011) pointed out that the strength matching effect changes considerably with notch locations. Weld metal overmatch decreases required toughness for the weld metal (WM) due to the shielding effect by the overmatch weld metal. Contrarily, the strength overmatch is not beneficial to HAZ toughness requirement because of the elevation of the stress field in the HAZ by the overmatch WM. They quantitatively estimated the influence of the strength matching and notch location considering the constraint loss correction factor, β, for CTOD.

3.2 Propagation of Brittle Fracture

Propagation of brittle fracture in welded steel structures could bring about sudden fatal accident. To avoid this type of fracture, double integrity concept, i.e. prevention of brittle fracture initiation and propagation, can be applied. For securing the arrest of brittle crack, once initiated, crack arrest toughness should be greater than crack driving force for a propagating crack. For this, improvement of crack arrest toughness of steels should be necessary along with measures to reduce crack driving force by e.g. improvement of weld geometries or reduction of residual stresses. Development of standard test methods for crack arrest toughness measurement is also an important issue.

3.2.1 Investigation of Brittle Fracture Propagation and Arrest

Kawabata *et al.* (2010) conducted a series of 500 mm wide crack arrest tests for establishing a standard test method for brittle crack arrest toughness, K_{ca}, of steel plates for ship structures. Effects of thickness and width of tab plate, distance between loading pins, temperature gradient and crack length on K_{ca} values were investigated for 16, 50 and 80 mm thick low carbon steel plates, together with numerical simulations of dynamic crack propagation by FEM. They determined the testing conditions for obtaining consistent K_{ca} values; tab plate thickness shall not be larger than 1.5 times that of test plate, tab plate width shall not be larger than 2 times that of test plate, distance between the loading pins shall be larger than 1.5 m for up to 350 mm crack propagation in the test plate, temperature gradient of the test plate shall not be greater than $0.25^\circ\, C/mm$. These conditions were determined so that reflection of stress waves at tab plates or loading pins does not interfere with crack propagation and that the test gives a K_{ca} value consistent with that of duplex type crack arrest tests, which has no temperature gradient, Figure 13. For theoretical and numerical analyses, see Prabel *et al.* (2008) and Menouillard *et al.* (2010).

3.2.2 Cleavage and Brittle Fracture Propagation in Welded Components

It was previously conceived that a brittle crack which is initiated from a weld tends to deviate from the weld into the base metal due to tensile residual stress in the welding direction. However, Inoue *et al.* (2006) demonstrated that a crack propagates along a large heat-input welded joint of heavy gage plate. A Japanese research consortium conducted a series of large scale tests for investigating the behaviours of dynamic crack propagation and arrest in welded steel components and for establishing a criterion of brittle crack arrest. Handa *et al.* (2010) conducted a series of duplex type crack arrest tests and structural component model tests, simulating hatch-side coming and deck structure, using 60 and 75 mm thick high-strength steel plates. The structural

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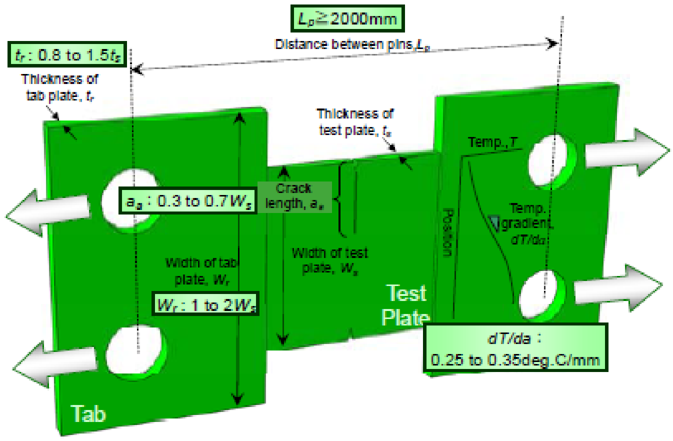

Figure 13: Proposed crack arrest testing conditions, Kawabata *et al.* (2010).

component model test under an applied stress of 257 MPa confirmed that, assuming weld penetration of the deck, brittle cracks can be arrested if the K_{ca} value of the test plate is 5,000 $N/mm^{3/2}$ or higher. In contrast, in the duplex type crack arrest tests of the same material, cracks propagated even if the K_{ca} value was 5,000 $N/mm^{3/2}$. This result indicate that the conditions for crack arrest are more relaxed in the structural component model tests than in the duplex type crack arrest tests, in which crack arrest was achieved with K_{ca} value larger than 6,000 $N/mm^{3/2}$ or higher. The test results also suggested the possibility that steel plates with heavier thicknesses needs higher K_{ca} value for crack arrest, Figure 14. Further, Inoue *et al.* (2010) conducted real-scale structural component model tests as well as ultra-wide duplex type crack arrest tests using 60 mm and 75 mm thick TMCP steels for clarifying the critical conditions for brittle crack arrest. They found that required K_{ca} value for crack arrest in the ultra-wide duplex test was 6,000 $N/mm^{3/2}$ or higher. Accounting for the structural discontinuity in the component considered, K_{ca} value of 6,000 $N/mm^{3/2}$

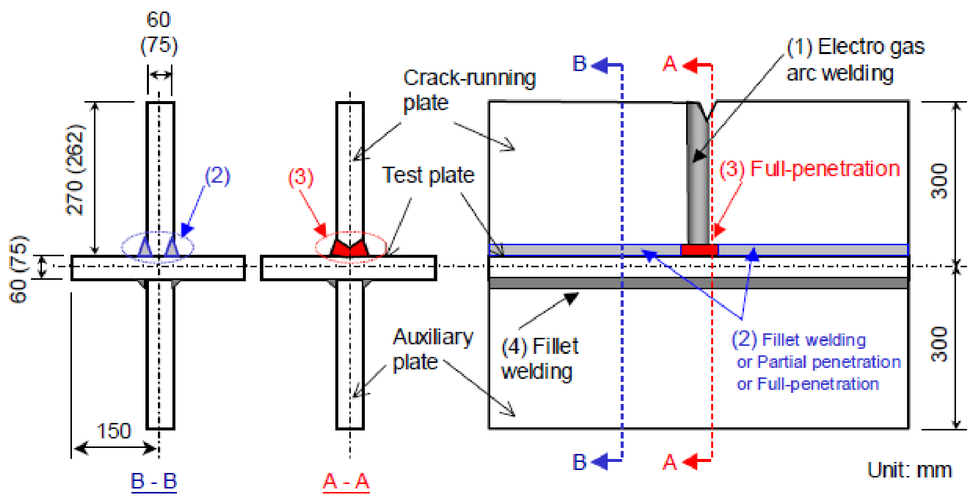

Figure 14: Structural component model tests for crack propagation and arrest, Handa *et al.* (2010).

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(a) LC-T1-1 (Arrest) (b) LC-T2-1 (Propagate)

Figure 15: Real scale structural component model tests for crack propagation and arrest, Inoue *et al.* (2010).

was considered sufficient for brittle crack arrest in the hatch-side coaming and deck structure, Figure 15.

An *et al.* (2008) conducted wide-plate crack arrest tests with temperature-gradient for large heat-input electro-gas welded joint of 50 and 80 mm thick plates. They obtained K_{ca} values 3,190 and 2,350 $N/mm^{3/2}$ for the both thicknesses at $-10°\,C$, which were about 40 % of those of the base plates. The brittle crack propagation paths were near the weld metal or fusion line, implying that the crack path was independent of the plate thickness. A crack arrest design strongly depends on whether the crack propagates along the weld or it deviates into base metal. Nakai *et al.* (2011) developed a three-dimensional numerical simulation model for brittle crack propagation path in the weld having toughness heterogeneity and welding residual stress distribution. Their model showed that the crack path is determined by a competition between stress state including residual stress and applied stress and toughness heterogeneity. Parametric calculations showed that a crack tends to deviate from the weld having shallower toughness valley at fusion line and under lower applied stress, which agreed well with the published data.

3.2.3 Example: Prevention of Brittle Fracture Propagation in a Container Ship

By applying relatively thick steel plates to large container ships, establishing technical requirements to prevent brittle fracture has become an urgent issue. Comprehensive studies on brittle crack propagation were carried out by a Japanese research consortium, Yamaguchi *et al.* (2010). As a result of this project, guidelines were developed with the aim to prevent large-scale failure of the hull structure by arresting brittle cracks at specific locations in the hull when such cracks were initiated unexpectedly. Their research outcome is summarized as; (a) brittle crack arrest toughness K_{ca} required for arresting long crack is 6,000 $N/mm^{3/2}$ for the actual structure of a container ship, (b) 300 mm is in general used as the required butt weld-shift. The developed guidelines are to set forth clear functional requirements for brittle crack arrest design in hatch-side coaming and deck structure in large container ships, Figure 16.

A Japanese joint research project, which is focusing on the safety-related issue of extremely thick steel plate applied to hull of large container ships, was conducted, Sumi *et al.* (2010). The project encompassed the prevention of brittle crack initiation from the welds as well as of brittle crack propagation, as mentioned above. The project proposed toughness requirement, K_c, as 4,000 $N/mm^{3/2}$ determined by deeply cracked

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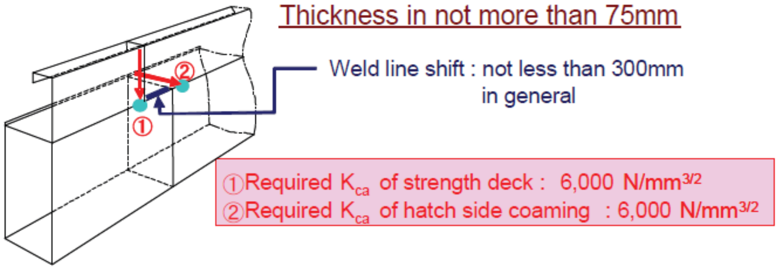

Figure 16: Guidelines for preventing brittle crack propagation in large containership structures, Yamaguchi *et al.* (2010a).

wide plate tests for the welds with plate thickness exceeding 50 mm, along with extensive analyses on fatigue crack propagation from assumed initial defects during service period. A number of ultrasonic testing was also conducted at shipyards for welds containing artificial defects to analyse the probabilistic nature of non-destructive testing. Regarding the prevention of brittle crack initiation, Shin *et al.* (2011) made fatigue crack propagation analysis for Fracture Crack Arrest, FCA butt-welded joint of hatch-side coaming, together with BS7910 brittle fracture assessment analysis. Minimum CTOD requirement for butt-welds was determined as 0.1 mm.

Doerk and Rörup (2009) investigated the toughness and quality requirements for YP47 steel welds (yield strength of 460) based on fracture mechanics. The authors investigated the effects of different influence parameters such as design temperature, fracture toughness, initial defect size, and shape of load spectra. By studying these parameters by fracture mechanics calculations, a safety concept for the avoidance of brittle fracture in YP47 welds was established. They concluded that for near surface defects "Inspection Based Design" during service life was necessary in order to prevent failure. Improvement of weld geometry for preventing brittle crack propagation in large container ships was studied by Toyoda *et al.* (2008) who proposed an unwelded breadth at the intersections between hatch-side coaming and strength deck, and confirmed the effect of the unwelded part by large-scale fracture testing and FEM analysis. They applied this concept to actual ship using butt-shift and chill plate. An *et al.* (2011) developed a brittle crack arrest technique in FCAW) and the combined process of EGW and FCAW using the arrest weld in the end of hatch-side coaming weld. They confirmed by large-scale testing that this concept could arrest a brittle crack without butt-shift in hatch side-coaming.

4 ADVANCES IN MATERIALS AND STRUCTURE DETAILS

The occurrence of fatigue, stress-corrosion cracking, and fracture in ship structures, particularly in high-strength steels, are now well recognized. The possibility of successfully dealing with these phenomena in ship structures are much better established now. Several advances in technology are currently available for analytical treatment and control. Advanced ship structures, which are presently emerging, such as high performance hydrofoil craft, involve the use of steels with significantly higher strength/density properties than the steels that have been traditionally used in the ship building industry. These new ship designs contemplate the structural application of alloys, such as 17-4 PH Stainless Steels, SY-130 steel, as well as 5000-series aluminium alloys and titanium.

Metal fatigue involves the initiation and growth of cracks under cyclic stresses where residual stresses remaining from manufacturing play an aggravating role. Metal fatigue

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takes on greater importance with increasing yield strength steels as they offer inferior fatigue crack growth resistance. Fracture is well acknowledged phenomenon to ship construction. Again, the problem applied to advanced ships is of brittleness associated with increasing yield strength, rather than brittleness associated with decreasing service temperature. For fracture, metallurgical and geometric factors become of primary concern. The embrittling effects of higher yield strength can be offset through metallurgical control, and the thinner section sizes associated with high-performance ships are less prone to brittle fracture than ordinary heavy section ship materials because of their greater ability for localized plastic deformation around crack tips.

4.1 Examples of Materials in Use

The structural steel specifications for commercial ships are developed and put into operation by a number of ship classification societies, including the American Bureau of Shipping (ABS) and the American Society for Testing and Materials (ASTM). These organizations have unified their requirements for structural steels into two classes: normal strength (235 MPa yield strength) and higher strength (317 and 352 MPa yield strength) (ASM: Carbon & Alloy Steels, 1996). The normal strength class consists of four grades of carbon-manganese steel, with the grading based on toughness. The higher strength class is also based on toughness, but they belong to a separate family of microalloyed high-strength low-alloy (HSLA) steels. Precipitation hardening mechanisms and grain refinement through the presence of small amounts of vanadium, niobium, and/or copper elevate their yield strength.

Aluminium is commonly used in other marine applications as well. These structures include main strength members such as hulls, deckhouses, and other applications such as stack enclosures, hatch covers, windows, air ports, accommodation ladders, gangways, bulkheads, deck plates, ventilation equipment, lifesaving equipment, hardware, fuel tanks, and bright trim (ASM: Aluminium, 1993). Aluminium-manganese (5xxx), and aluminium manganese-silicon (6xxx) alloys have been widely used for ship superstructures due to light weight and excellent corrosion properties. High strength aluminium-copper (2xxx) and aluminium-zinc-manganese (7xxx) alloys can also be used in marine atmospheres.

4.2 Extra High Strength Steels

The use of high strength steel has seen an increased demand lately in order to save weight and welding time, especially the pipeline industry is driving the research of high strength steel development, Shimamura *et al.* (2011) and Li *et al.* (2010). Welding high strength steel can be a challenge especially in order to obtain good toughness properties in the weld metal and HAZ since the weldability of steels depends on its hardenability. Hamada *et al.* (2009) investigated the tensile and Charpy properties of girth welds for high strength linepipe, they concluded that the use of dual torch provided better toughness values than a single torch which provided higher yield strength. They also recommended that further research should be put into investigating the carbon content in the weld metal, since they discovered that a somewhat higher carbon content lead to higher fraction of intergranular acicular ferrite which indicated that the tensile and toughness balance was improved.

Common fatigue design is independent of the materials yield strength, however in the low-cycle range, plastic deformation occurs due to high loading and the effect of the yield strength will have an effect on the achieved life. Fatigue codes put restrictions on allowable yield strength, e.g. DNV-RP-C203, allows steel for YS up to 960 MPa in

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air and 550 MPa for steel materials in seawater with cathodic protection or steel with free corrosion. Herion *et al.* (2011) concluded that the butt welds made of S690QL and S960QL gave much better results than the corresponding detail category or FAT-class (DC71-DC-80 according to EN 1993 1-9 (2010)), while S1100QL and S1300QL provide lower fatigue strength. The latter results were to due lack of adequate weld material, in addition the heat input during welding influences the strength of the materials negatively. Hrabowski and Herion (2009) investigated the effect of post weld heat treatment on steel grades S700MC, S960QL and S1100QL by using high frequency peening methods. They found that improvement gets even higher with increasing yield strength of the steel. The same effect was seen by Ummenhofer *et al.* (2011) who investigated the effect of high frequency hammer peening of 960 MPa steel and saw a 66 % increase in fatigue strength of the treated joints compared to the as welded joints.

4.3 Steel with Improved Crack Growth Properties

It is often assumed that the fatigue crack growth properties of structural steels fall in a common scatterband like in BS 7910. However studies have shown that improvement of the microstructure of the steel can lead to an increased life for the component in question. Katsumoto *et al.* (2005) and Konda *et al.* (2007) showed that the fatigue crack growth rates of fracture crack arrester (FCA) steel were reduced compared to conventional steel in side longitudinal structural model and side wide gusset welded joints. They also showed that the stress concentration at the weld toe of a FCA steel specimen is smaller than that of a conventional steel specimen. The steel has already been applied to some ships. The developed steel plate has been approved as FCA in grades AH36, DH36, EH36 and AH40, DH40, EH40 by Nippon Kaiji Kyokai, Lloyd's Register, Det Norske Veritas and American Bureau of Shipping. Sakano *et al.* (2005) investigated the fatigue life extension effect of FCA steel through fatigue tests using welded girder specimens made of FCA steel and a conventional high strength steel (JIS SM570Q), with welded joints between cross beam bottom flanges and main girder web. They showed that the fatigue strength of web gusset joint of FCA steel specimens is about 1.3 times higher than that of conventional steel specimens. Youn *et al.* (2007) carried out fatigue tests for FC and conventional steel weldments. They found that the fatigue strength of FCA steel weldment is in the upper region of the fatigue strength data band of conventional steel weldments, and the fatigue crack propagation rate in HAZ of FCA steel weld is smaller than that of the base metal of conventional steel.

Nakashima *et al.* (2005) examined the relation between fatigue crack growth rate and fatigue life of welded joint in steel with dispersed secondary phase. Fatigue crack growth tests of base metals and fatigue tests of gusset welded joint were carried out. They found that the fatigue life increases by 10 % when the growth rate decreases by half, and the fatigue life doubles when the growth rate decreases by a factor of 10. The developed fatigue crack arrester (FCA) steel can provide a new alternative for fatigue life improvement measures from the viewpoints of materials. Tentative design S-N curve for the FCA steel was newly defined, Hara *et al.* (2010), Konda *et al.* (2010). The steel was tentatively used in a stress-concentrated area in an LNG carrier. The proposed design S-N curve makes it easy to use FCA steels without any scantling increase or structural reinforcements resulting in steel weight increase. Thus, long target fatigue lives may be documented without applying post weld improvements like weld toe grinding, Figure 17.

Fatigue crack arrest (FCA) steel can be explained by the two following mechanics;

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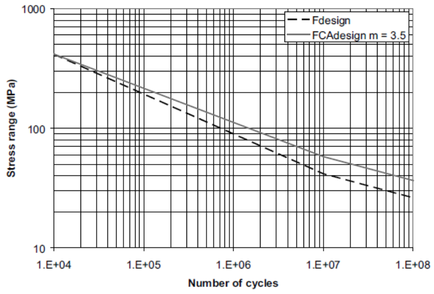

Figure 17: Tentatively determined S-N curves for Fatigue Crack Arrester steel, Konda *et al.* (2010).

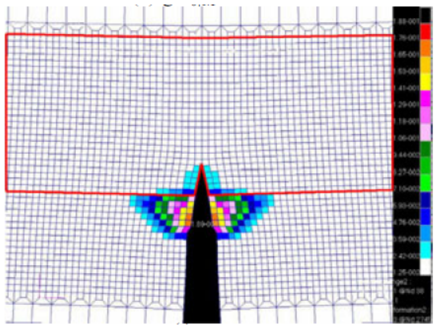

Figure 18: Meso-scopic FEM showing fatigue crack growth retardation at martensite phase in DP steel, Osawa *et al.* (2008).

- Increased fatigue initiation resistance at weld HAZ by suitable micro structures.
- Decreased crack growth rate in base material when a fatigue crack passes a grain boundary from a soft phase (feritte) to a hard phase (bainite) that is present in these new dual phase steels

Koda *et al.* (2010) carried out fatigue testing of FCA steel and a design S-N curve has been proposed based on 66 small scale tests of FCA steel and 18 tests from test specimens made from conventional steel. From the S-N test data they discovered that the slope of S-N curves for FCA steel and conventional steel is different, where insignificant effect was seen in the high stress area and benefit is seen in the high cycle region. The paper concluded that the fatigue life could be increased by a factor of 3 for a typical ship structure subjected to typical long term stress range distribution from wave actions.

The microstructures and fatigue crack growth rates (FCGRs) of EH36 thermo-mechanical control process (TMCP) steel weldments have been reported by Tsay *et al.* (1999). They show a modest increase in hardness of the heat-affected zone (HAZ) in the as-welded condition due to the low carbon equivalent (CE) of the steel. Microstructural observations indicate that the coarse-grained HAZ is composed of mainly lower bainite with some upper bainite. Fine-grained HAZ consisted of refined ferrite and bainite, together with interlath microphases. Although the impact toughness of specimens vary significantly with orientations with respect to the rolling direction, minor change in FCGRs of the TMCP steel plate was found. The lower FCGRs of the HAZ than those of the steel plate is attributed to the formation of low-carbon bainite with high toughness, regardless of the postweld heat treatment (PWHT). The evidence showed that the TMCP steel weld after tempering at $600^{\circ}\,C$ for 2 h possessed a better resistance to crack growth than the plain-C steel plate.

Fatigue life improvement by microstructural control of steel has been studied by meso-scopic and crystal plasticity based FEM, Osawa *et al.* (2008). Crack closure level was found to increase as a crack-tip approaches martensite-phase boundary in a ferrite-martensite dual phase (FMDP) steel. This change became more pronounced with martensite phase with higher hardness. Furthermore, the calculation results led to a prediction that the crack growth rate of the FMDP steels with polygonal and banded martensite phase becomes much higher than that with flattened and banded martensite phase because the cracks can slip through narrow slit between the martensite phases, Figure 18. Possible fatigue life prolongation by grain size refinement, Go-

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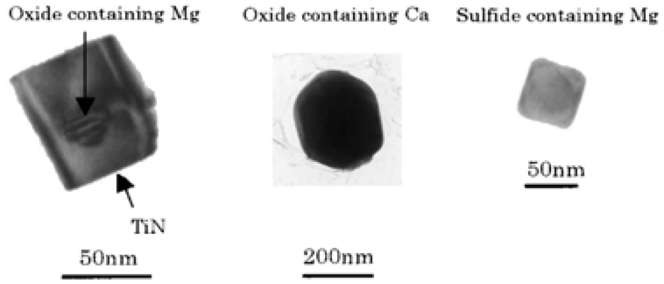

Figure 19: Pinning particles in steel for preventing HAZ grain growh, Shirahata *et al.* (2011).

toh *et al.* (2009), and the importance of the softening behavior of crystal and elastic property of inclusions, Tsutsumi *et al.* (2009), were predicted by numerical calculations.

4.4 Materials for Brittle Fracture Susceptible Structures

For realizing the double integrity against brittle fracture, it is indispensable to improve material's resistance to brittle fracture initiation at weld as well as brittle crack propagation at base metal through microstructural control of HAZ and base metal, respectively. YP460MPa class heavy section steel plates have been developed through strict control of rolling and cooling practice in the TMCP process, Otani *et al.* (2011), Kaneko *et al.* (2010), An *et al.* (2011). These steels were subjected to the temperature-gradient type crack arrest tests, showing K_{ca} values higher than 6,000 $N/mm^{3/2}$ at $-10° C$. Some of these steels have been applied to large container ships. Improvement of HAZ toughness are targeted to high heat input electro-gas welding and medium to low heat input multi-pass welding, the former being applied to butt-welding of thick section components in large container ships and the latter to offshore structures. Fine oxide and sulphide particles are dispersed in steel to prevent grain growth at HAZ of large heat-input welding, Kaneko *et al.* (2010), Shirahata *et al.* (2011), Figure 19. Fukunaga *et al.* (2010) presented the second generation Ti-oxide steels, which contains high amount of manganese, enhancing intragranular ferrite (IGF) transformation from Ti-oxide particles and suppressing ferrite side plate (FSP) microstructure at grain boundaries, both decreasing the effective grain size of HAZ and achieving high HAZ CTOD values at $-40° C$, Figure 20. Suh *et al.* (2011) proposed a new parameter of carbon equivalent type, representing HAZ toughness, Figure 21.

4.5 Materials for Cold Climate

Oil and gas exploration and production is moving into arctic areas. The reduction in ice-covered areas has rendered northern routes more advantages and in addition it is anticipated that as much as 25 % of the undiscovered oil and gas resources can be found in the Arctic. This means additional challenges for design, construction and operation of offshore installations and ships. In addition, very low temperatures are required for storage and transport of LNG. These low temperatures influence the fatigue and fracture properties of the material. An effort has been taken by the Barents 2020 project, where the goal is to harmonise industry standards for health, safety and the environment for the Barents Sea, where both Russian and Norwegian participants are involved, Sæbø and Cammaert (2011). However, none of these rules and guidelines is derived specifically for the arctic and cold temperature applications. Hence, the oil

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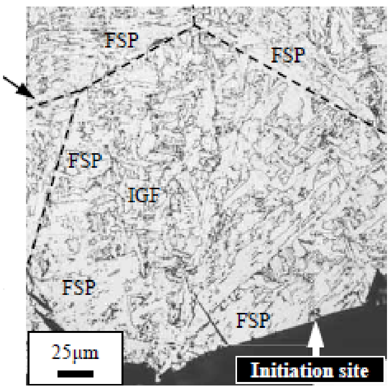

Figure 20: HAZ microstructure of the second generation Ti-O steel, Fukunaga *et al.* (2010).

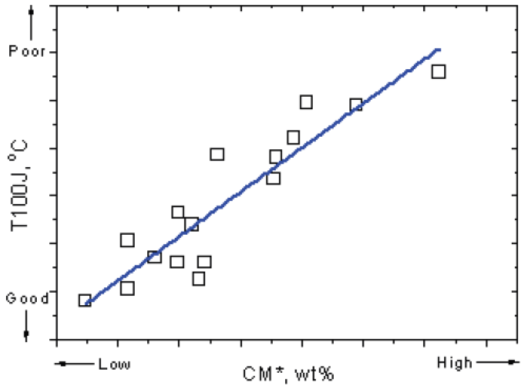

Figure 21: A proposed new alloying element parameter representing HAZ toughness, Suh *et al.* (2011)

and gas industry is looking for recommendations and guidelines for material selection and design for low operating temperatures.

A five year Arctic material research project supported by The Research Council of Norway, oil companies, offshore suppliers and contractors is investigating criteria and solutions for safe and cost-effective application of materials for hydrocarbon exploration and production in Arctic regions. One main task for the project is to carry out material and toughness testing of carbon steel at $-60° C$ in order to qualify steel for low temperature applications and the main results were presented at the First Arctic Material Symposium at ISOPE2011. The results from the project showed a large scatter in the toughness results for 355 MPa and 420 MPa steels, Østby *et al.* (2011), Akselsen *et al.* (2011)and Welch *et al.* (2011). A probabilistic fracture mechanics evaluation was presented by Horn and Hauge (2011) in order to investigate the severity of the obtained fracture toughness distributions and the effect of the constraint of the different test specimens when it comes to structural integrity. The same toughness data was evaluated statistically by Hauge and Holm (2011) who presented a statistical interpretation of the toughness test data by looking at different models and they concluded that the SINTAP procedure provides in general a conservative estimate of the lower tail of the statistical distribution.

4.6 Honeycomb Structures

The application of lightweight honeycomb panels as the elements of ship structure provides benefits like; 20-30 % reduction in weight and material consumption as compared to traditional structures for like equal bearing capacity as conventional solutions. The application of laser welding makes the manufacturing of honeycomb structures processable and effective, Rybin (2010). The CRISM (Central Research Institute of Structural Materials) “Prometey” has developed a sequential series of welding procedures for laser and arc-laser welding providing the possibility of manufacturing honeycomb panels and ship structure components.

From the three point bending tests on the aluminium honeycomb sandwich beam specimen varying the honeycomb core cell thickness, it was observed that with an increase in the thickness of honeycomb core cell, the start of plastic deformation could be delayed, resulting in increase of ultimate strength. Also, the sandwich beam bending stiffness subsequent to plastic buckling becomes more moderate as the thickness of

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honeycomb core cell increases. This would imply that undesirable effects of instability in the structure after collapse can be reduced by using a larger thickness of core (Paik *et al.,* 1999).

5 DAMAGE CONTROL AND RISK-BASED ASSESSMENT

Fatigue assessment of welded structural components is associated with significant uncertainty in assessing fatigue life and the use of reliability theory and statistical methods. Uncertainties arise from both the loading and the resistance assessments. Fatigue uncertainties depend on the intended service life, nature of in-service inspection, availability and quality of S-N data and environmental loads data, and assessment methods employed. The objective of this section, which is discussed for the first time by this committee, is to review the recent developments in fatigue damage control and risk based assessment of marine structures. The effects of workmanship, inspection and quality and weld improvement with respect to fatigue strength are considered. The present status of uncertainties, reliability and risk related to fatigue damage are also reviewed.

5.1 Effect of Workmanship, Internal Defects, Welding Procedure, etc.

Welded joints are potential sites for initiation of a fatigue crack. A poor workmanship that can reduce the performance of a detail is weld spatter, unauthorised accidental arc strikes, attachments, corrosion pitting, weld flaws, poor fit-up, eccentricity and misalignment. The results of experimental and numerical studies aimed at defining the effects of weld penetration, root gap and misalignment on the fatigue resistance of cruciform structural details fabricated from steel have been presented by Polezhayeva and Dickin (2010). The investigation includes a literature survey on the subject from which relevant experimental data is incorporated to support recommendations. Based on the results obtained, recommendations were made for weld parameters and fit-up to achieve optimal fatigue performance, especially with regard to defining crack initiation location. The ratio of lack of penetration (LOP) to effective throat has been proposed as a means of determining crack initiation location. The effective throat is the fused weld throat measured from the end of the LOP perpendicular to the assumed straight weld surface. It has been concluded that for two-sided welded load-carrying T and cruciform joints, no failure occurs from the weld root for LOP/effective throat less than or equal to 0.5. For one-sided welded load-carrying joints, failure from the weld root can still occur at LOP/effective throat less than 0.5.

Chakarov *et al.* (2007) evaluated structural deterioration due to corrosion and the correlation between the status of degradation and stress concentration factor have been established. A probabilistic study of the effect of uncertain weld shape on the structural hot-spot stress distribution around the weld toe using a Monte-Carlo simulation and the finite element analysis has been presented by Gaspar *et al.* (2011). A structural detail consisting of a plate strip with a transversal butt welded joint and a tapered thickness step is used as case study. The results demonstrated that the SCF increases considerably if weld shape imperfections are considered. Normally both fatigue and corrosion will be present and their combined effect needs to be considered in that the decreased net section due to corrosion will increase the stress levels, which in turn increase the rate of crack growth (Garbatov *et al.,* 2002). The welding of ship structures is regarded as a process that requires a high level of control to develop finished product consistency. By its nature, welds may still contain low levels of defects. To reduce defect levels and improve the consistency of the welded products several factors have been identified by McPherson (2010) contributing to inferior performance.

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Some of these have been termed "management issues", i.e., technology and aspects that are well established and need to be part of the overall managed process. In addition, the differentiation between thick and thin plate has been made, highlighting their significantly different requirements.

To alleviate the stringent workmanship requirement on the conventional complete joint penetration (CJP) welds for tubular offshore structures. Qian *et al.* (2009) propose a new set of enhanced partial joint penetration (PJP+) welding details, which utilize a part of the brace wall as the inherent backing plate for the welding procedure. The PJP+ welds have similar SCF values as the CJP welds. The experimental results reported by Marshall *et al.* (2010) on a series of large-scale circular hollow section X-joints confirmed the satisfactory performance of the PJP+ welds, which demonstrate a fatigue life exceeding 10 times that estimated using the S-N curve in API RP 2A developed for tubular joints fabricated using the complete joint penetration welds.

Design should be performed such that fatigue cracking from the root is less likely than from the toe region. The reason for this is that a fatigue crack at the toe can be found by in-service inspection while a fatigue crack starting at the root cannot be discovered before the crack has grown through the weld. Fricke (2011) summarizes several approaches like the nominal stress approach, the structural stress approach, effective notch stress and the notch stress intensity approaches and stress propagation for fatigue assessment of weld roots. The different approaches have been applied to six examples, where minimum two approaches for each case have been evaluated (cruciform joints, fillet weld around attachment end, one-sided fillet weld around RHS joint, lap joint and cover plates, fillet-welded pipe penetration and laser-stake welded T-joint). The different cases were partly tested so that the results could be compared with experiments.

The possibility to specify the quality levels according to the requirements of fatigue design is of a high economic relevance. Hobbacher and Kassner (2010) describe the actual state of the art in terms of consistency of quality groups in ISO 5817 with fatigue properties. This direct relation gives the possibility to specify a weld quality for required fatigue strength and vice versa. They propose to revise the quality groups in the direction that for normal butt welds a quality group of B and for fillet welds a quality group of D might be sufficient. A correlation of the fatigue properties with the quality groups of ISO 5817 have been established at the example of 10 mm wall thickness and are conservative at higher wall thicknesses and throats too is proposed.

More details about the effect of workmanship, internal defects, welding procedure, etc. can be found out in the report of ISSC V.3 committee.

5.2 Inspections and Quality

The requirement of fatigue strength for design has to account for weld quality, which can be achieved at economically justifiable cost and by inspection of the structure. In addition to visual inspection, two methods of non-destructive testing are commonly used to detect the size of defect: radiography and ultrasonic. The acceptance criteria imply a corresponding limit on defect size and because of that there is a need to measure the size of detected defects. Knowing how likely a flaw will be found during an inspection is important for many reasons: as feedback to design, to provide guidance in setting inspection schedules, and as a common ground upon which to compare different inspection technologies. Inspection systems are inevitably driven to their extreme capability for finding small flaws. When applied to this extreme, not all flaws of the same size will be detected. In fact, repeat inspections of the same flaw will

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not necessary produce consistent hit or miss indications, and different size may have different detection probabilities. Because of this uncertainty in the inspection process, capability is characterized in terms of the probability of detection as a function of flaw size.

Berens and Hovey (1983) discussed the statistical nature of the NDE process and the different ways used for the estimate of POD, which could be from Hit/Miss data or from signal response data. He discussed also, the design of NDE reliability experiments and analysis illustrating the different factors affecting it and sample size requirements for the different POD estimation methods. Demsetz *et al.* (1996) investigated the different means of developing POD curves for marine structures, including the different factors affecting it. They gathered information regarding inspection practice and inspection performance in marine and in other industries. Then, they identified and developed methods for evaluating inspection performance. They estimated the POD curves for common inspection procedures and details, the costs of inspection for various inspection types and structures, and made quantitative estimates of the probability of detection of corrosion damage and the accuracy of measurement of such damage. The continuous degradation of a material due to both fatigue and corrosion has been studied by among others Brennan *et al.* (2008) who described the use of inspection reliability information in fitness-for-service and criticality assessments for ship and offshore structures. They adopt a concept of Probability of Detection (POD) and Probability of Sizing (POS) information with associated confidence measures into damage modelling. With this approach, operators can appreciate the benefit of conducting inspections and the resulting implications for quantitative risk assessments particularly where no defects are found. Their work also addresses the emerging trend towards monitoring with inspection and how operators and designers can benefit from future trends in structural health monitoring; see also Garbatov and Guedes Soares (2009a), Garbatov and Guedes Soares (2009b) and Ivanov (2009).

5.3 Fatigue Improvements

Upgrading the fatigue performance of a welded structure can be achieved by good detail design and by improvement methods, and a lot of research is carried out in this field and summed up in IIW Recommendations on Post Weld Fatigue Life Improvement of Steel and Aluminium Structures, which was revised 2010, Haagensen and Maddox (2010). The improvement methods can be classified as weld geometry modification methods and residual stress methods. The two methods used in industrial applications are weld toe grinding, such as a disk grinder or a rotary burr tool; tungsten inert gas (TIG) re-melting of the weld toe region; weld profile control, i.e. performing the welding such that the overall weld shape gives a low stress concentration and the weld metal blends smoothly with the plate; special electrodes with good wetting characteristics to give a favourable weld toe geometry; hammer peening, needle peening, shot peening and UIT of the weld toe region.

Tai and Miki (2011) studied the improvement effects of fatigue strength by hammer peening treatment on out-of-plane gusset plate specimens. They found that the improvement effect by hammer peening treatment under constant amplitude loading was quite high, especially in lower stress areas; however this effect was not seen for variable amplitude loading. One of the recent improvement methods and probably the most effective one is high frequency hammer peening. Le Quilliec *et al.* (2011) showed experimental results relating to high frequency hammer peening and concluded that developing a consistent and robust approach with the aim of defining the optimum

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operating conditions of the process is important. Maddox (2010b) compared three peening methods on virtually identical fillet welded specimens and concluded that the obtained improvement in fatigue strength were identical for 4-pass hammer peening, 4-pass needle peening and shot peening using condition E (0.8 mm diameter steel shot producing an Almen intensity of 0.024 to 0.028 A2). Mori *et al.* (2011) investigated the effect of UIT (Ultrasonic Impact Treatment) which is a weld toe improvement method by introducing compressive residual by impact on weld toe with hard pins vibrated by ultrasonic energy. From their study they concluded that significant increase in fatigue strength can be obtained by this method for low stress ratio, however the effect cannot be obtained in high stress ratio since the effect is mainly derived from compressive residual stress and not due to the modification of weld toe shape. The same technique was also investigated by Okawa *et al.* (2011) and they concluded that the fatigue strength of UIT treated welded joint was slightly decreased after application of preload, however, the benefit of UIT is significantly greater than that of grinding. Martinez (2010) investigated the fatigue test results for ultrasonic peening treated welds, and found a factor 4 in fatigue life extension compared to as-welded joints.

A probabilistic approach of high-cycle fatigue behaviour prediction of welded joints is presented by Sghaier *et al.* (2010). The approach takes into account the surface modifications induced by welding and the post-welding shot peening treatment. The Crossland criterion is used and adopted to the case of welded and shot peened welded parts and the reliability computation results are presented as iso-probabilistic Crossland diagrams for different welding and shot peening surface conditions. The approach is applied for validation of a butt-welded joint made of S550MC high strength steel. A comparison between the computed reliabilities and the experimental investigations show good agreement.

The weld profile improvement method is included in the AWS/API design rules in terms of the X curve that may be used generally if profile control is carried out. In the HSE UK S-N the curves for all types of joints can be moved by a factor of 1.3 on strength (2.2 on life) if grinding is carried out. With reference to the requirements about the fatigue design stipulated in the IACS CSR and based on the experience in plan approval and operation, fatigue design codes for the places in which fatigue often takes place are discussed by Zhan and Chen (2010). TIG (tungsten inert gas) dressing as a post-weld improvement techniques are advised to be supplemented into CSR because the weld toe is re-melted in order to remove the weld toe undercut or other irregularities and to reduce the stress concentration of the weld transition.

5.4 Uncertainty in Fatigue Assessment Diagrams and Fatigue Life Assessments

The SLA (Safety Level Approach) based GBS (Goal-Based new ship construction Standard) in IMO is seen as a representative instance for assessing the safety level. Developing risk-based method based on the SRA (Structural Reliability Analysis) for ship structural safety assessment has been a purpose of the study presented by Choung *et al.* (2010). Special focus was given on the development of several limit state functions and related uncertainties. The fatigue safety level, which corresponds to the reliability index or probability of failure, was calculated for each limit state functions as a result, the feasibility of applying risk-based approach to the design and safety assessment of ship structure was examined and demonstrated. The final crack size to be employed as the upper limit for the fracture mechanics analysis is

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taken as either equal to the plate thickness or to the crack size, which would cause an unacceptable risk of failure. The failure modes usually considered are fracture or plastic collapse. The stress intensity factor is used as the parameter controlling stress, strain, and energy fields in the neighbourhood of a crack tip. For materials, which fail by fracture under linear elastic conditions, the failure occurs at a critical value of the stress intensity factor. The stress intensity factor loses its validity as a linear elastic parameter in plastic region contained by surrounding elastic material. To analyse such situations properly it is necessary to carry out elastic-plastic stress analyses of cracked detail and to use alternative parameters to measure the severity of crack tip conditions such as the crack opening displacement and the J contour integral.

The methodology utilizes a failure assessment diagram (FAD) to determine if a failure has taken place, which assumes that failure will occur through either of two mechanisms: brittle fracture or plastic collapse. The main concept of the FAD is to provide a boundary curve that represents the locus of predicted failure points. Calculated points lying inside the boundary curve are assumed to be safe. Any cracks lying outside the boundary curve are considered to be unsafe. One of the most commonly used FADs is given by BS7910 (2005), for the assessment of flaws in metallic structures.

5.5 Suitability and Uncertainty of Physical Models - Reliability and Risk Assessment

In current practice, designs are chosen to satisfy specific safety and durability life and strength requirements by analyses of damage growth and residual strength. Decisions on materials, structural configurations, allowable stresses, etc. are based on the results of these analyses.

Analyses of crack growth damage, while deterministic, rely on input data such as initial flaw sizes, material and usage variability, etc. which are available in a statistical format. In addition to predicting mean values of damage accumulation, the methods can be used to predict the distribution of damage with time. The results of the probabilistic analyses are extremely sensitive to the initial distributions of the variables and the functions used to approximate them. Among the major variables to be considered are: material strength, crack growth rate, critical crack size, operational loads, crack detection capability, inspection techniques, frequency of inspection etc.

A probabilistic study of the effect of the weld shape imperfections on the structural hot-spot stress distribution along the weld toe using the Monte-Carlo simulation and finite element analysis method has been presented by Gaspar *et al.* (2009). To analyse the uncertainties of fatigue damage of welded structural joints a different approach has been employed by Garbatov and Guedes Soares (2010).

There are many published reliability methods in the literature. In fact, the sparsity of data associated with the major variables and the extreme sensitivity of the results to the distribution functions has limited the effectiveness and the confidence in the results. Furthermore, the design of a specified reliability level requires that the acceptable failure rate is established in advance, and that relationships should be established between reliability and normal design decision factors. For the concept of durability, probabilistic techniques may have the most direct impact, since the ultimate decision on the economic life is connected to the global population of cracks.

This problem is one of the major threats to the structural integrity of deteriorating ship structures; Garbatov and Guedes Soares (1998), Guedes Soares and Garbatov (1998) and Akpan *et al.* (2002). Due to randomness and epistemic uncertainties associated

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Table 1: Stochastic model

Variable	Distribution	Mean Value	Standard Deviation
Δ	Log-Normal	1.0	0.3
$\tilde{a}$	Log-Normal	5.754E+12	1.726E+12
B_L	Normal	0.85	0.255
B_s	Normal	1.0	0.12
B_H	Normal	1.0	0.20
B_Q	Normal	1.0	0.20
m	Deterministic	3	-
λ	Deterministic	0.900	-
μ	Deterministic	0.450	-

with the action of sea water waves and the sea environment as well as operation, fabrication, and modelling of ship structures, a probabilistic approach has to be applied to assess and predict their fatigue performance. The First Order and Second Order Reliability method (FORM/SORM) provide a way of evaluating the reliability efficiently with reasonably good accuracy, which is adequate for practical applications. Fatigue assessment of a tanker ship hull converted to a FPSO structure has been used here to demonstrate the use of FORM/SORM technique based on the S-N fatigue damage approach.

Garbatov *et al.* (2004) studied an upgrade of a FPSO planned for a 25 year service life after being converted from an oil tanker that operated 20 years in the North Atlantic. Full spectral fatigue damage for the FPSO was performed by the use of a hot-spot stress analysis, see Table 1 for the input parameters used in the evaluation. The formula used can be found in (Garbatov *et al.* 2004) and they are similar to equations 7.6 and 7.7 applied for the case study.

Table 2 presents the results of the fatigue reliability assessment of two details (HS1, HS2) of the oil tanker operating without restrictions during 20 years. The table also shows the reliability results assuming that the ship operates more 25 years in the North Atlantic as an oil tanker in addition to her normal service life of 20 years. The importance of the contribution of each variable to the uncertainty of the limit state function $g(x)$ can be assessed by the sensitivity factors which are determined by, see Garbatov *et al.* (2004) for details:

$$\alpha_i = -\frac{1}{\sqrt{\sum_{i=1}^{\infty}\left(\frac{\partial g(\underline{x})}{\partial x_i}\right)^2}}\frac{\partial g(\underline{x})}{\partial x_i} \tag{4}$$

5.6 Ageing and Aged Ships and Offshore Structures

Many operating ships and offshore installations have now been in service for more than 25 years. Numerous of these structures are approaching or have exceeded their original design lives (50 % of the fixed platforms in the UK section have exceeded their

Table 2: Reliability indices of the oil tanker in the North Atlantic

	Tanker (20y)		Tanker (20+25y)+	
	HS 1	HS 2	HS 1	HS 2
P_f	2.17E-2	1.11E-1	9.67E-2	2.90E-1
β	2.019	1.219	1.300	0.552

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original design life, Stacey, 2011). Both the Petroleum Safety Authority (PSA) in Norway and the HSE offshore division in the UK have worked intensively on defining requirements for safe operation of offshore structures extending their original design lives. As input for the development in the UK, a survey of industry practices used in other sectors were conducted and reported by Sharp *et al.* (2011) including experience from operation of air crafts, nuclear and process plants, bridges and ships. It was concluded that the industries are dealing with the ageing in a way that commensurate with the resulting hazards and risks and that the offshore industry could benefit from the development of similar consistent and integrated approaches that are present in some of the other industries. Yamamoto *et al.* (2007) propose a fatigue management system using a sensor called the "hull ageing management system" and propose it as a pro-active safety management system for LNG carrier hull structural ageing and introduce a method to improve the accuracy of accumulated fatigue damage detection by using this sensor.

The problems related to assessing the serviceability and safety of aged steel ships including the assessment of the structural condition methods for repair, quantification of strength of deteriorated and repaired ships accounting for the uncertainties involved and cost-benefit and risk-based decision procedures for remedial actions have been the main objective in three consecutive reports: Bruce *et al.* (2003), Paik *et al.* (2006) and Wang *et al.* (2009).

In the North Sea, the number of fixed platforms exceeding their original design life is steady increasing with time. A structural reliability assessment tool was therefore developed by Gupta *et al.* (2011) with propose of identifying the most critical structures among the ageing platforms in the UK sector. Basic parameters such as age, building quality and possible erosion of air gap were included in the model. A detailed overview of issues related to ageing and management of Lifetime Extension (LE) of offshore facilities and an outline of corresponding overall risk assessment is presented in Hokstand *et al.* (2010). The report considers three aspects of ageing; (1) material degradation, (2) obsolescence, i.e. operations or technology being "out of date" and (3) organisational issues. The proposed LE process consists of six main activities for the assessment of the overall risk pictures. In the UK, the KP4 programme, "Ageing and life extension inspection programme for offshore installations" has been embarked by the HSE, which intend to move duty holders towards more proactive means in the management of ageing and life extension of offshore structures. An outline of the KP4 Asset Integrity Management system is presented in Stacey (2011). The principal elements of the management system are outlined being; (1) Policy, (2) Organisation, (3) Planning and Implementation, (4) Measurement of Performance and (5) Audit and Review and key issues for ageing installations are discussed.

Material degradation has a significant impact on the ageing of the structures and the evaluation of their safety. During an evaluation of possible life extension there are several key questions which have to be considered related to the material including original design, operation, as-is conditions and future conditions. It is therefore important that material and corrosion engineers get involved in the life extension. Hörnlund *et al.* (2011) emphasise that considerations shall be given to both known but also new and unexpected degradation mechanisms. A practical example of a lifetime extension of an offshore structure is presented by Haagensen *et al.* (2011), who described some of the challenges involved in the life extension of the floating production unit – Veslefrikk B. For this platform, the LE comprised repair of already existing cracks, inserted of cast details where the structure is especially fatigue prone and weld

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improvement of existing weld seams. A probabilistic fracture mechanics model was used to estimate future fatigue crack occurrence in critical braces.

- More testing of existing facilities that are decommissioned in order to learn from actual material degradation
- Development of material related key performance indicators
- International development of best practice for lifetime extension of platforms

6 DESIGN METHODS FOR SHIP AND OFFSHORE STRUCTURES

This chapter gives a review on design methods and codes which are applicable to the design of marine and offshore structures. The last ISSC Fatigue and Fracture Committee has given a comprehensive review on the topics in the ISSC Conference in 2009 (ISSC, 2009). Therefore, this report is focusing on the recent developments.

6.1 Design Codes

6.1.1 Common Structural Rules (CSR) for Oil Tankers and Bulk Carriers (2009, 2010, 2011)

The CSR are the result of the combined knowledge, experience and latest technical expertise of the world's leading classification societies. The CSR for Oil Tankers and Bulk Carriers can be applied to either double hull oil tankers or bulk carriers classed with the Society and contracted for construction on or after 1st of April 2006. The common structural rules (CSR) for oil tankers (OT) and bulk carriers (BC) began at different points in time and initially followed individual paths of developments. Some concepts from both the rules are discussed below with their similarities and differences:

- *Application*: The CSR applies to double hull oil tankers of 150 m in length and above, and to single and double-side-skin bulk carriers of 90 m in length or above. Both the CSR for OT and BC depend on the geometric characteristics of the ship such as 'L/B' ratio, 'B/D' ratio and block coefficient 'C_b' etc. The locations to be assessed for OT and BC are longitudinal stiffener end connections and primary structural joints (e.g. hopper knuckles and horizontal stringer ends for OT and hopper knuckles and hatch corners for BC) in the cargo hold region. Under the common structural rules, the basic design condition is 25 year design life in North Atlantic wave environment.
- *Design loads for fatigue requirements (Load Approach)*: CSR-OT uses envelope load method, in which the load assessment is based on the expected load history. The expected load history for the design life is characterized by the 10^{-4} probability level of the dynamic load value; the load history for each structural member is represented by Weibull probability distributions. For CSR-BC, the equivalent design wave (EDW) method is used to set the design loads.
- *Notch Stress approach*: The CSR-BC rules requires the notch stress to be obtained by multiplying the hot spot stress with a notch stress factor which is dependent on the weld type (i.e. butt or fillet weld) and weld treatment. It is assumed deep penetration welding falls under the category of fillet weld. The notch stress approach is not used in the CSR-OT rules.
- *Hot Spot Stress Definition*: The stress read out position for BC is defined at the structural intersection at the mid plane of the transverse attachment and at the edge of the longitudinal attachment, requiring an extrapolation of the element stresses closest to the vicinity of the hotspot. The OT rules procedure on the other hand define the stress read out point at the weld toe, requiring an interpolation (or extrapolation) of the element stress closest to the vicinity

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of the hotspot depending on weld leg length. The BC rules define the hotspot stress as the principal stress within 45° of the normal to the crack, while the OT rules define this as the stress normal to the weld.

- *Stress determination for stiffener connections*: The BC rules permit three methods for the fatigue stress assessment of stiffener connections, namely, direct method, superimposition method and simplified method. The OT rules generally expect only the "simplified" method to be applied, but does provide the option for "direct" method where the configuration of the end connections are substantially different from those shown in the rules.
- *Stress due to hull girder moments*: The main difference is that the BC rules obtains the hull girder hot spot stress by multiplying the nominal stress with a stress concentration factor for hull girder loads, while the OT rules operate with the nominal stress only. The OT rules also stipulates the hull girder stress to be obtained from an inertia based on 0.25 corrosion deduction, while BC rules applies one based on 0.5 corrosion deduction.
- *Stress due to local load*: The main difference is that the BC Rules apply a stress concentration factor (SCF) to the nominal stress to obtain the hotspot stress while the OT rules operate on the nominal stress directly. It is also noticed that the BC rules permit the stress concentration factors to be determined from FE analysis in lieu of the prescribed values in the rules. The OT rules accept this only where the attachment configuration is not covered in the rules.
- *Stress Combination approach*: The BC rules adopt an equivalent design wave approach where the load components are applied to the Finite Element model concurrently to represent head seas, following seas and beam seas conditions; and the hot spot stress are obtained directly for each of these design wave condition. The BC Rules term this the "direct method". The OT rules adopt an envelope load approach for fatigue in which case the component loads are applied individually to the FE model and the stresses from these component load cases are combined to obtain the hotspot stress. No particular terminology is given by the OT rules for this approach since this is the only approach permitted for determining hot spot stress.
- *Simplified hot spot stress method for hopper knuckle*: The BC rules provide an approach where the hot spot stress in way of the hopper knuckle can be determined using nominal stresses from a coarse mesh finite element model and stress concentration factors in lieu of fine mesh finite element models. The OT rules do not contain such provisions.
- *Long-term distribution of stress range*: A two-parameter Weibull function is used for long term stress distribution in both CSR rules. But, the presentation is different: the law is given in form of cumulative probability density function for CSR BC whereas CSR OT gives it in the form of a probability density function. CSR BC considers a Weibull shape parameter (ξ) which is equal to unity whereas CSR OT considers a Weibull shape parameter which depends on the ships length, on the type of member to be assessed and on the transversal position of the member.

6.1.2 *Harmonized Common Structural Rules (CSR-H) for Oil Tankers and Bulk Carriers (status at 2012, scheduled for publishing in 2014)*

The initially published versions of the Rules (CSR) for Oil Tankers and Bulk Carriers were developed separately and were based on different technical approaches. IACS is harmonizing the Common Structural Rule (CSR) for Oil Tankers and Bulk Carriers

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into a single set of rule book (at the moment using CSR-H to distinguish from currently applicable CSR rules). The CSR-H will consist of three parts; a common part for "general hull requirements" that will contain requirements for both ship types, and separate parts for "ship type specific" requirements applicable to Oil Tankers and Bulk Carriers respectively. Noting that the CSR were developed and adopted prior to the substantive development of the Goal Based Standard (GBS) provisions in IMO, the development of CSR-H provides an opportunity for IACS to consider and take account of the discussions and decisions taken in the development of the GBS. The development of CSR-H is also taking account of the experience gained in the application of the separate CSR, including feedback from industry partners. The goal of the CSR-H remains the same as the current CSR development, which is to establish unified rules and procedures for safer and robust ships, but now also includes the formal consideration of the IMO GBS. As the CSR-H is still under the development and the currently planned date for publishing is 1st of July 2014 (subject to change).

6.1.3 Fatigue Assessment due to Springing and Whipping

Whipping and springing caused fatigue on ships, particularly for large container ships, is becoming a concern for the industry. Major classifications including Lloyd's Register (2009) are under taking study and investigation on the issue. ABS (2010) released guidance notes on whipping and spring assessment for container carriers which include fatigue assessments. DNV (2007) gives guidance Note of CN no. 30.7 related to the assessment of the additional fatigue effect of wave induced vibrations of the hull girder.

6.1.4 Rules for Offshore Installations (Risers, Pipelines, FPSOs)

Rules for Risers

Riser systems exposed to ocean currents may experience in-line as well cross-flow Vortex Induced Vibrations (VIV). For VIV assessment the application of Computational Fluid Dynamics (CFD) has been explained in DNV-OSS-302 'Offshore Riser Systems' (2010). It suggests that the time domain fatigue damage assessment should be based on a recognised cycle counting approach, typically Rain Flow Counting (RFC).

An Offshore Standard guiding rule DNV-OS-F201 (2010) 'Dynamic Risers' includes a section on the fatigue assessment of risers which are subjected to repeated fluctuations (with ref. DNV RP-C203). The Code gives details regarding fatigue analyses procedures, narrow band and wide band fatigue damage assessment procedures and fatigue capacity S-N curves.

DNV-RP-F204 (2010) a code for 'Riser Fatigue', presents recommendations in relation to riser fatigue analyses based on fatigue tests and fracture mechanics. The standard introduces the basic fatigue damage methodology and global fatigue analysis procedures for the wave frequency and the low frequency fatigue. It also discusses requirements for combining wave frequency, low frequency and VIV fatigue damage. The Recommended Practice DNV-RP-F202 (2010) document gives criteria, requirements and guidance on structural design and analysis of riser systems made of composite materials exposed to static and dynamic loading for use in the offshore petroleum and natural gas industries.

Rules for Pipelines and piping

As we know that there are several examples of piping applications exposed to severe cyclic loadings where the need for a more comprehensive fatigue assessment is evident, for instance piping exposed to wave loads such as wellhead piping on jacket platforms

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and expansion loops on bridges. In such applications NORSOK L-002 (2009) suggests to refer the fatigue assessment method outlined in PD5500, Annex C.

DNV-RP-D101 (2008) is a code that accounts for the acoustic fatigue due to the vibrations produced by machinery such as reciprocating pumps and compressors. It suggests the simplified fatigue analysis when there is more than one additional cyclic load source of importance to the expansion and contraction or alternating bending stresses of a piping system.

For the submarine pipeline systems (DNV-OS-F101, 2010), the fatigue check shall include both low-cycle fatigue and high-cycle fatigue. A deterministic or spectral analysis approach is used as most of the loads which contribute to fatigue.

Rules for FPSOs

The ABS code (2010) gives the guidelines for 'Spectral-Based Fatigue Analysis for Floating Platform Storage and Offloading (FPSO) Installations' (for the fatigue assessment) in terms of either expected damage or life. The fatigue strength of structural details is established using the S-N curve approach that is specified in the referenced guide.

ABS Guide for Floating Production Installations (FPI, 2010) covers the fatigue design considerations for the following: Ship-type Installations, Column-Stabilised Installation, Tension leg Platform/Spar Installation, Existing Vessel converted to FPI or FPI conversion, Trading Vessel, Import and Export System and Mooring Systems.

Offshore Standard rules and regulations for the various offshore installations are as follows

The recommended practice DNV-RP-C203 (2010) 'Fatigue Design of Offshore Steel Structures' provides a general guideline for the fatigue design of offshore structures including FEM modelling guidelines and S-N curves for the application of the notch stress approach. The RP has been updated on requirements to grinding for fatigue life improvement and a section on low cycle fatigue in combination with high cycle fatigue has been added.

The standard DNV-OS-C102 (2009) 'Structural Design of Offshore Ships' over rules DNV-OS-C101 (Design of Offshore steel structures), ships constructed in steel for any defined environmental condition. For the evaluation of the fatigue limit state (FLS) the standard suggests that the effect of all significant loads contributing to the fatigue damage shall be considered. And the fatigue life shall be calculated considering the combined effects of global and local structural response.

For the FLS design of more recent offshore installations such as substructures for wind turbines and corresponding substations, DNV have compiled the codes DNV-OS-J101 (2010) and DNV-OS-J201 (2009).

The ISO 19900 series tries to bridge the international society by provision of a common set of rules for the offshore design including fixed steel offshore structures ISO 19902 (2007) and topsides 19901-003 (2010).

Lloyd's Register ShipRight-FOI (2008) analysis procedure propose a procedure for calculating fatigue damage due to loading and unloading of Floating Offshore Installation and three levels fatigue assessment procedures.

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6.1.5 Arctic Design Codes

Lloyd's Register (2011) has released new procedures under the notation, ShipRight FDA ICE, for fatigue design assessment of ships navigated in ice regions. The Arctic is estimated to hold about 20 % of the world's remaining recoverable hydrocarbon reserves and greater trade through the Arctic is driving demand for larger ice-class vessels, particularly oil tankers and LNG carriers. It is reported by Zhang *et al.* (2011) that 57 % of ice-class ships have cracks or fractures at an average age of 13.0 years. It is becoming increasingly important that the industry develops a good understanding of the fatigue strength of hull structures under ice loading. The FDA ICE procedure includes the following steps: 1) determination of ice data for trading routes; 2) ice impact frequency; 3) ice load distribution; 4) determination of structural stresses under ice loads; 5) fatigue damage calculations and S-N curves, and 6) fatigue acceptance criteria.

The International Maritime Organization (IMO) has recently approved the "Polar Code", a guide for ships in polar waters operations invites countries to adhere to these rules on a voluntary basis to be in effect as of January 1st 2011 and established regulations for cruise ships that travel to Antarctica. For the design of offshore platforms in arctic regions, ISO 19906 (2010) specifies requirements and provides recommendations and guidance for the design, construction, transportation and installation. In the design of fixed structures in arctic areas special attention shall be made to the risk of dynamic ice-structure interaction known as frequency lock-in, or self-excited vibration, which can arise when a sheet of ice acts continuously on a vertical structure at a moderate ice speed. The steady-state vibrations that arise due to the lock-in phenomenon can cause low-cycle fatigue in steel structures, ISO 19906 (2010).

7 CASE STUDY

The benchmark study is intended to show uncertainties in treating fatigue assessment of welded structures when unconventional loading pattern is applied to a welded joint. The main aim of the study is to call attention of structural designers on the uncertainties associated to fatigue checking procedures in presence of stress multiaxiality even if they are referring to well consolidated standard guidelines, and to focus attention of researchers on the open question of how to treat stress multiaxiality. A present status of the design rule requirements, being differently harmonized with the most up-to-date scientific criteria, have been discussed. Unlike the benchmark studies presented in the past ISSC reports (ISSC, 2000 and ISSC, 2003), where the procedures of classification societies were compared to investigate uncertainties in the different modelling approaches for stress/strain calculation and/or for damage calculation, it is the aim of the present study to outline an unclear standard frame where contradictions in proposed methodologies appear.

The starting point of the study has been the identification of a well stated structural problem, which is briefly described below, in order to facilitate replication of analyses. The benchmark study has been carried out on a simple structural geometry with smooth discontinuities, where full penetration fillet weld has a standard section shape and cyclic loadings are clearly relatesd to hull girder bending. To reduce the source of scatter in determination of the fatigue life, a comparison of the different approaches has been made by applying the outcome of calculations carried out on a shared FE model in combination with a common load history. The common stress-strain field has been calculated based on the ideal scheme of no imperfections related to gaps and misalignments and by neglecting corrosion effects. Since the procedures

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applied are the ones given by standard practice, fatigue checking implemented in the comparative study is a linear approach. Hence, a linear elastic FE analysis for mapping the stress-strain field has been used. This comparative study makes reference mainly to guidelines proposed by the Classification Societies, but considers also alternative procedures proposed by the recent applied research.

7.1 Selection and Description of the Case Study

The evolution in cruise ship which has occurred in the last few years has led to ships which are more comfortable and have grown in size and offer cabins with balcony extending all over the ship's sides. The resulting increase in the number of decks, the extensive use of high tensile steel to reduce weight and obtain benefits in terms of stability, and the broader openings giving access to balcony from cabins, result in a weaker global structure of the ship. Furthermore, prefabricated sections do not necessarily contribute to the local structural needs, as in case of the intersection between passing-through decks and side shell (also called outer longitudinal bulkhead). That intersection is certainly a critical area on the structure of any large modern cruise ship. It is the selected structural detail for the case study. Of course, the opening edges are of highest importance for the fatigue strength. However, their evaluation is very well covered by the rules. The focus of this benchmark is set to multiaxial fatigue of weld seams.

7.1.1 Description of the Case study: Basic Data

The cruise ship selected for the study is a large ship of about 90,000 GT and a length of about 300 m, recently built in one of the major European shipyards. Such a modern cruise ship has been designed for worldwide service luxury cruises, ref. Figure 22. For the selected welded connection, the local stresses are emphasized by vertical hull girder deformation and by the large openings on the longitudinal bulkhead, which act as stress raisers. Stress pattern in front of the weld line on deck suggests the hot spot to be subject to a weakly biaxial stress field where highest tension stresses are parallel to the weld line. On the other hand, stress field in front of the weld line on longitudinal bulkhead is more general, as stress flow lines are controlled by shape of openings corners.

The welded joint considered for the fatigue analysis is a cruciform joint where the passing element is the deck plate, on which the longitudinal bulkhead plate is joined

Figure 22: Superstructure of a modern cruise ship during the building.

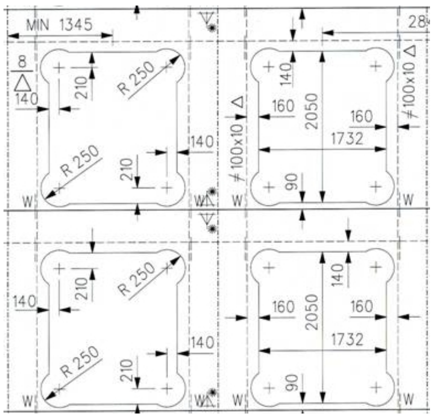

Figure 23: Particular of the drawing of the longitudinal bulkhead

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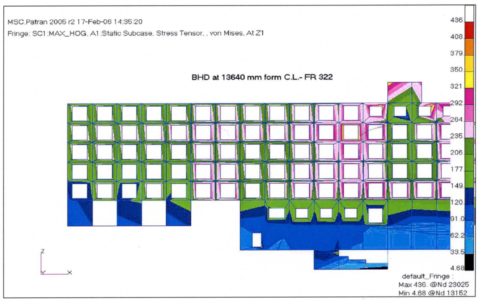

Figure 24: Particular of the coarse mesh FE model of the forward part of the outer longitudinal bulkhead.

Figure 25: Particular of the coarse mesh model and displacements collected for application to the boundaries of the intermediate model.

by a continuous manual longitudinal fully penetrated K-butt weld (45° single-V preparation). Thickness of deck plating at the intersection is $7\,mm$, reduced to $5.5\,mm$ at $110\,mm$ far from the weld line toward the balcony area, while the longitudinal bulkhead has a uniform thickness of $12\,mm$. The geometry of the openings on the outer longitudinal bulkhead is shown in Figure 23. The opening at the lower edge is close to the deck, especially at the lower corners with circular enlargement to reduce local stress concentrations. The whole structure is made by high tensile stress (minimum yield stress of $355\,MPa$).

Two hot spot locations have been identified as the most critical, both located at the continuous longitudinal weld between deck and longitudinal bulkhead, at the weld toes on deck and wall. Here, the longitudinal stresses due to vertical bending of the hull are transferred by the thick plates of the longitudinal bulkhead, through the longitudinal fillet weld, to the thin plates of the deck, causing the bending of the deck with a slight shear lag effect. So, stress concentration is expected at the weld on wall side and high stress levels are also possible on deck side.

Data on loading acting on the structure were made available by designers from a FE analysis. Due to the coarse mesh implemented in that model, where just four elements were used to model the outer longitudinal bulkhead around each opening, results are not accurate enough to identify the most critical area on the bulkhead. So, a refined model has been created to study the hot spot area.

The displacements applied to this intermediate model have been collected from the corresponding frame of the coarse mesh model, Figure 25 in the area were the highest stresses were detected for the two extreme cases of hogging and sagging of the ship's hull. Only translational deflections have been considered. Such load cases are based on the IACS vertical wave bending moment formulations. Hence all calculated stresses are related to the same probability of exceedance. All FEM calculations have been performed by using Patran/Nastran software.

7.1.2 Description of the Case Study: Local Stress Calculation

The intermediate sub-model has been obtained by cutting off the longitudinal bulkhead for an extension in height and length equal to four openings (each opening module is $2840\,mm$ long and $2720\,mm$ height), and with a transverse extension of deck equal

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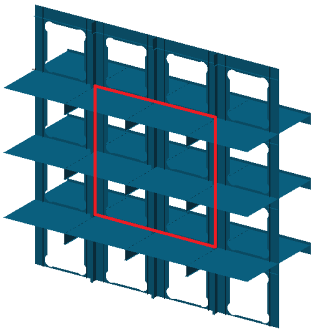

Figure 26: Intermediate FE sub-model.

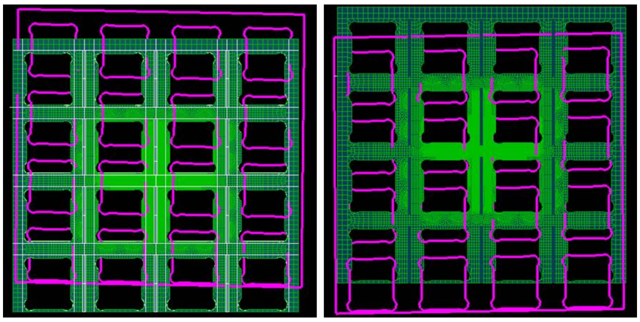

Figure 27: Deformations due to sagging and hogging on the intermediate model.

to about 2200 mm both inside and outside the longitudinal bulkhead, as shown in Figure 26. The mesh size has been set to accurately describe structural geometry, such as opening corners. The average dimension of elements is of about 100 mm. 8-node quadrilateral shell elements have been used, arranged in the mid-plane of the structural components. Displacements relevant to both the maximum hogging and sagging conditions at the boundary of the sub-model have been provided by the global hull model and have been applied to each intersection between the longitudinal bulkhead, frame and deck on the model boundary. The global stresses and displacements have emerged from the analysis of the intermediate sub-model and hogging and sagging effects are clearly visible from the deformations of the sub-model structure, ref. Figure 27, where primary stress flows are locally amplified at each opening. Stresses are higher at the lower edges of the openings both on deck and wall side.

The sub-sub model (local structure model) is like a double cross obtained from cutting off the longitudinal bulkhead at the intersection between the deck beam and the side shell frame, see Figure 28, for an extension in height and length equal to about a half of the opening height and length respectively, and with a reduced transverse extension of the deck both inside and outside the longitudinal bulkhead. The model includes the main supports of the longitudinal bulkhead and deck. The mesh size on each different part of the structure has been set to suit the relevant plate thickness and, at the hot spot, 8-node quadrilateral shell elements have been used, about $t \times t$ size; The full penetration weld seam itself required no extra effort in geometric model building. Boundary displacements relevant to sagging and hogging condition of the ship have been applied on all the nodes along the cut-off line of the model. Displacements have been adapted to the finer mesh by linear interpolation among given values.

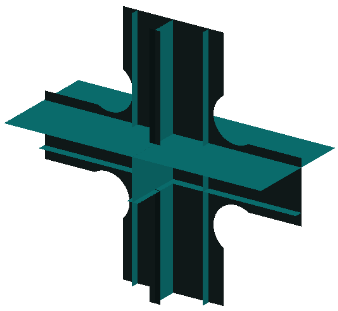

Figure 28: Local structure sub-model (view from inside the ship).

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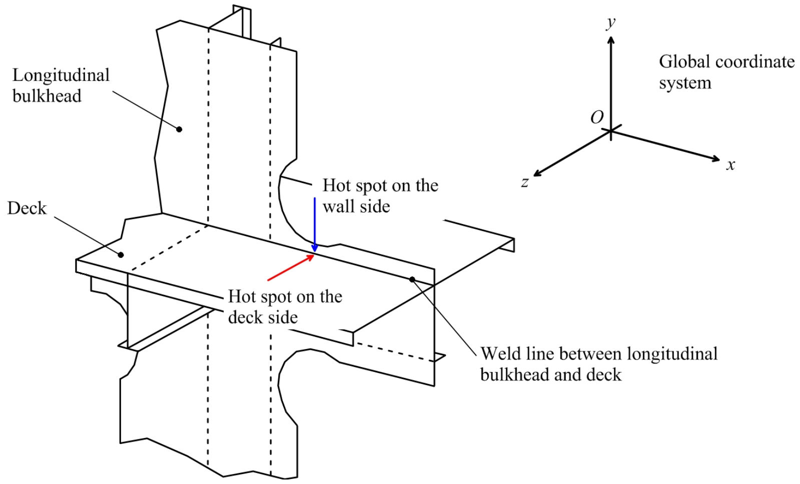

Figure 29: Particular of the structure near the hot spot area (view from outside the ship).

By solving the local structure model for the two loading cases, the stress ranges have been calculated at the weld joint between deck and longitudinal bulkhead: the outer weld toe line on deck and the upper weld toe line on bulkhead, They have been identified as the most critical areas.

A nodal line perpendicular to the weld line has been selected where stress intensities are the largest both on deck and wall (basing on general assumption regarding the hot spot extrapolation, hot spot stresses have been evaluated in different points near the selected critical point). Figure 29 shows a of part of the modelled structure, along with the location of the two hot spots and corresponding nodal lines. The nodal lines where stresses have been read out are located about 280 mm and 210 mm far from the vertical stiffener adjacent to the longitudinal bulkhead opening, for deck and bulkhead hot spot respectively. Hot spots are both located at the round corner of the opening, where the distance of the opening edge from the weld seam is of about 50 mm and 60 mm, for deck and bulkhead hot spot respectively. On both intermediate and local structure models, boundary effect on read out areas have been checked and congruence between the models has also been verified.

Both nodal stresses and element stresses have been collected at the selected nodal lines, to be used for hot spot extrapolation according to the analysis method. The stresses given in the following are referred to the global coordinate system, where x-axis is longitudinal from stern to bow and parallel to the weld toe line of the selected weld, y-axis is vertical from bottom to upper deck and z-axis is oriented outside ship. Outputs are given for the two layers of the elements: layer 1 lies on the upper surface of deck plate and on the outer surface of longitudinal bulkhead plate. Stresses are reported on the nodes and elements sorted by distance d_{HS} in mm from model hot spot (i.e. the idealized weld toe line). Element stresses have been averaged on values relevant to elements aligned on both sides of the nodal lines. The element type is Patran/Nastran SHELL8, featuring membrane plus bending capability. The stresses for the two ship's extreme design loading conditions (vertical hogging and sagging of the hull girder) are given in MPa in Table 3 and Table 4 for the two hot spot locations in components and as Von Mises stresses (σ_{VM}), both referred to the global coordinate system.

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Table 3: Stresses calculated at the weld toe line on longitudinal bulkhead plate.

load case	stress type	layer	d_{HS}	σ_{VM}	$\sigma_x = \sigma_{\|\|}$	$\sigma_y = \sigma_{\perp}$	$\tau_{xy} = \tau_{\|\|}$
hogging	nodal	1	2.56	101.26	87.87	38.91	-38.45
			5.13	103.45	90.55	38.69	-38.76
			7.69	105.85	93.40	38.35	-39.12
			10.25	108.33	96.16	38.12	-39.57
		2	2.56	102.21	88.41	40.66	-39.03
			5.13	104.43	91.07	40.38	-39.40
			7.69	106.84	93.90	40.00	-39.80
			10.25	109.33	96.65	39.72	-40.29
	element	1	2.56	101.19	88.01	38.72	-38.30
			7.69	105.80	93.56	38.14	-38.95
			12.82	110.95	99.41	37.43	-39.77
			17.95	116.68	105.61	36.55	-40.75
		2	2.56	102.14	88.56	40.46	-38.88
			7.69	106.78	94.07	39.78	-39.63
			12.82	111.96	99.89	38.98	-40.54
			17.95	117.71	106.06	38.00	-41.60
sagging	nodal	1	2.56	67.88	-58.59	-26.45	25.98
			5.13	69.27	-60.30	-26.26	26.17
			7.69	70.78	-62.14	-25.99	26.38
			10.25	72.34	-63.91	-25.79	26.65
		2	2.56	68.71	-58.98	-27.74	26.51
			5.13	70.11	-60.68	-27.50	26.73
			7.69	71.62	-62.51	-27.20	26.97
			10.25	73.19	-64.27	-26.97	27.27
	element	1	2.56	67.84	-58.68	-26.32	25.89
			7.69	70.74	-62.25	-25.85	26.27
			12.82	73.99	-66.00	-25.28	26.76
			17.95	77.61	-69.98	-24.61	27.34
		2	2.56	68.67	-59.08	-27.61	26.42
			7.69	71.59	-62.62	-27.06	26.86
			12.82	74.85	-66.35	-26.43	27.41
			17.95	78.48	-70.31	-25.69	28.05

7.2 Method of Investigation

7.2.1 Method Applied for Reference Stress Calculation

Within the cumulative damage procedure, the fatigue design of any structural detail is performed by making use of a reference parameter s derived by the stress-strain field measured or calculated in close vicinity of the crack site. The fatigue check is performed by comparing s with the strength value S, which is endurable at the same number of cycles N.

The local approach has been implemented based on the structural stress analysis. The "structural stress approach" ($s = \sigma_s$) takes into account only that part of the local stress concentration which is related to the structural geometry and allows to explicitly consider multiaxiality in the local stress field in front of the hot spot. In plate-type structures, the structural stress σ_s is commonly defined as the sum of a membrane and a bending stress at the hot spot, excluding the local nonlinear peak stress at the notch. The surface stress extrapolation is carried out on the stresses readout points which are located sufficiently far from the weld seam.

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Table 4: Stresses calculated at the weld toe line on deck plate.

load case	stress type	layer	d_{HS}	σ_{VM}	$\sigma_x = \sigma_{\|\|}$	$\sigma_y = \sigma_\perp$	$\tau_{xy} = \tau_{\|\|}$
hogging	nodal	1	3.50	79.88	74.37	-10.00	1.47
			7.01	78.41	73.19	-9.49	1.53
			10.51	77.10	72.14	-9.04	1.58
			14.02	75.79	71.08	-8.59	1.63
		2	3.50	83.74	78.23	-10.07	0.91
			7.01	82.28	77.06	-9.57	0.93
			10.51	80.98	76.00	-9.15	0.94
			14.02	79.67	74.93	-8.72	0.96
	element	1	3.50	79.84	74.33	-9.99	1.47
			7.01	77.08	72.11	-9.03	1.58
			10.51	74.59	70.10	-8.18	1.67
			14.02	72.34	68.28	-7.42	1.73
		2	3.50	83.71	78.20	-10.06	0.91
			7.01	80.95	75.97	-9.15	0.95
			10.51	78.48	73.95	-8.35	0.97
			14.02	76.25	72.11	-7.66	0.99
sagging	nodal	1	3.50	53.25	-49.68	6.48	-0.96
			7.01	52.30	-48.92	6.15	-1.00
			10.51	51.45	-48.23	5.86	-1.02
			14.02	50.59	-47.54	5.57	-1.05
		2	3.50	55.47	-51.97	6.43	-0.49
			7.01	54.52	-51.21	6.10	-0.50
			10.51	53.68	-50.52	5.82	-0.51
			14.02	52.83	-49.83	5.54	-0.52
	element	1	3.50	53.23	-49.66	6.48	-0.96
			7.01	51.43	-48.21	5.86	-1.03
			10.51	49.81	-46.90	5.31	-1.07
			14.02	48.35	-45.72	4.83	-1.11
		2	3.50	55.45	-51.95	6.42	-0.49
			7.01	53.66	-50.51	5.82	-0.51
			10.51	52.06	-49.20	5.30	-0.53
			14.02	50.61	-48.01	4.85	-0.54

7.2.2 Methods Applied for Stress Multiaxiality

When a welded joint is subject to a loading having a major component acting parallel to the weld line, fatigue cracking may no longer initiate along the weld toe, but initiates in the weld and then rotates to grow normal to the maximum principal stress direction. This means that the parallel stress has a significant role in the fatigue capacity of a structural detail and the notch at the weld toe does no longer significantly influence the fatigue capacity. Hence it becomes conservative to use the principal stress range together with an S-N curve for stress range normal to the weld toe.

Several methods have been proposed for taking into account stress multiaxiality. With reference to the load carrying penetrating fillet and butt welds, the biaxial-stress hypothesis represents the most general approach for fatigue assessments. There are mainly two ways to consider a biaxial stress state: reference principal stresses or the single components of the stress tensor, that is the tensile stress $\sigma_\perp$ acting normally to the weld, the weld parallel normal stress $\sigma_{||}$ (usually disregarded is such kind of approaches) and the weld parallel shear stress $\tau_{||}$. According to the case study, procedures set for proportional loading (i.e., in-phase stress components) are presented.

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The multiaxial fatigue damage for brittle materials may be calculated by referring to the principal stresses. Hence, the fatigue effective equivalent stress and the fatigue check criterion are defined as (σ_a means stress amplitude):

$$\sigma_{a,eq} = \frac{\sigma_{a\perp} + \sigma_{a\|}}{2} \pm \frac{1}{2}\sqrt{(\sigma_{a\perp} - \sigma_{a\|}) + 4\tau_{a\|}^2} \quad \text{and} \quad D \leq D_{per} \tag{5}$$

where D_{per} is the permissible damage sum. In general, according to this approach, the principal stress to be used should be approximately in line with the perpendicular to the weld toe, i.e. within a deviation β ranging from $\pm 45°$ to $\pm 60°$ (see Figure 3, where σ_1 and σ_2 are the principal stresses). Alternatively, the maximum principal stress is accounted for in the fatigue evaluation.

For ductile materials is often suggested to refer to the criterion of the distortion strain energy (generally referred to as the von Mises yield criterion) or to the criterion of the maximum shear stress (also known as the Tresca criterion). In both cases, the fatigue effective equivalent stress and the fatigue check criterion are expressed by:

$$\sigma_{a,eq} = \sqrt{\sigma_{a\perp}^2 + \alpha\tau_{a\|}^2} \quad \text{and} \quad D \leq D_{per} \tag{6}$$

where $\alpha = 3$ and $\alpha = 4$ for the Von Mises and the Tresca criterion respectively.

A similar relationship has also been proposed where α is set equal to 1, resulting in Eq. 7 to express the equivalent stress as the so called "resultant stress" σ_{res}, which is obtained by vector addition of the normal and shear stresses at the relevant part of the weld toe. The background is fracture mechanics studies of the growth of fatigue cracks inclined to the direction of loading, Maddox (2010). Resultant stress may be defined considering normal stresses irrespective of being parallel or perpendicular to weld toe line direction. So, when a weld is subject to a uniform uniaxial far field stress of amplitude $\sigma_{a,0}$, and θ is the angle between nominal applied stress and perpendicular to weld line, the weld may be assessed as a transverse weld if $\sigma_\perp$ is the dominant normal stress or as longitudinal weld if $\sigma_{||}$ is the dominant normal stress. So σ_{res} comes out to be respectively:

$$\begin{aligned} \sigma_{a,eq} &= \sigma_{a,res} = \sqrt{\sigma_{a\perp}^2 + \tau_{a\|}^2} = \sigma_{a,0}\cos\theta \quad \text{(transverse weld)} \\ \sigma_{a,eq} &= \sigma_{a,res} = \sqrt{\sigma_{a\|}^2 + \tau_{a\|}^2} = \sigma_{a,0}\sin\theta \quad \text{(longitudinal weld)} \end{aligned} \tag{7}$$

Another approach applied in the case of proportional biaxial variable-amplitude loading is based on a linear superposition of the damages caused by normal and shear stresses as acting independently. A simplified method for summing up normal and shear stress cumulative damages is to calculate them separately and adding their values powered to 2/3 please note: 2/3 is not consistent with Eq. 8. This results in the following equation (Bäckström, 2003; Hobbacher, 2009):

$$\left(\frac{\sigma_{a\perp,E}}{\sigma_{A\perp}}\right)^2 + \left(\frac{\tau_{a\|,E}}{\tau_{A\|}}\right)^2 = D_E^2 \leq D_{E,per}^2 \tag{8}$$

where the index E refers to the "equivalent stress amplitude" which is the constant amplitude stress equivalent, in terms of fatigue damage and the index A refers to the permissible stress amplitude at the same number of cycles N_L. Here also the concept of strength reduction parameter D_E is introduced, defined as the ratio between the fatigue effective equivalent stress and permissible stress amplitude acting perpendicular to the weld line. That relationship may also be applied for $\sigma_{||}$ instead $\sigma_\perp$.

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To generalize the formulation given in Eq. 8, the relative weight of shear stress effect to normal stress effect needs to be balanced. That is made by correcting the shear stress by a coefficient γ prior to enter the equation, so obtaining a formulation formally equal to that of Eq. 6. It has been proposed (Lotsberg as cited in Maddox, 2010) to deduce γ values by comparing design S-N curves for failure in shear with those for failure under normal stress. The assumption $\gamma = 1$ means equivalent stress as the resultant stress σ_{res}. Values in the range from 0.20 to 0.81 depending on the weld detail are proposed by Lotsberg and thus the effective stress reflects the assumption of less influence of the shear stress.

In all the mentioned criteria, the cumulative damage is calculated according to S-N curves defined for uniaxial loading. As for in-phase loading on steel structures, the value of the permissible damage sum D_{per} is generally limited to 1.0 or 0.5, while $D_{E,per}$ is fixed to 1.0. The fatigue effective equivalent stresses calculated according to the discussed methods are suitable for structural stress approach.

7.2.3 Method Applied for Cumulative Damage Calculation

As for the load history, all analysed approaches consider the use of a simplified statistical method for long term distribution of wave loads which may be implemented according to the Weibull probability density function $p_W(2s_a)$, where s_a is stress amplitude. When the significant value of s_a has been calculated together with its probability of exceedance $Q(2s_a)$, the load history may be approximated by the product $N_L \cdot p_W(2s_a)$, where N_L is the total number of cycles experienced by the ship during her entire lifetime. This approach assumes that the Weibull shape parameter k_W can be deduced by proper considerations on the type and location of the structural detail, and the Weibull scale parameter λ_W is derived as function of s_a, $Q(2s_a)$ and k_W.

For practical purposes, the S-N curve is generally simplified in a one-slope curve, defined by the two parameters m and $\log c$. Alternatively, also a bi-linear S-N curve with change in slope at certain knee point is used.

7.3 The Fatigue Life Prediction

Calculation carried out by the local structure model for the deck side shows a hot spot stress characterized by a weak biaxial state where ratio between $\sigma_{||}$ to $\sigma_{\perp}$ stresses is of about 13 % and ratio between $\tau_{||}$ and $\sigma_{\perp}$ is approximately 1 %. (Again, the edges are significantly higher loaded but not of interest here as their evaluation is well covered by rules.) The longitudinal normal stress $\sigma_{||}$ is by far the predominant stress at the hot spot. As for such stress component, a stress increase is hardly detectable along the nodal lines perpendicular to the weld line. On the other hand, at the wall side, the hot spot stress is characterized by a high biaxial state where the ratio between $\sigma_{||}$ to $\sigma_{\perp}$ stresses is of about 48 % and the ratio between $\tau_{||}$ and $\sigma_{\perp}$ is about 45 %. In this case, all stress components may play a significant role on fatigue damage. Procedures defined by different classification societies and by IIW have been applied in the present study and methods and outcomes are discussed in the following. All procedures refer to fatigue crack growth from the weld toe into the base material. It is also common practice in guidelines to assume a design life of 20 years and the limit value for the accumulated fatigue damage to 1.0. All procedures provide indications for a simplified calculation of the load history and give formulations to establish a proper value for the Weibull shape parameter λ. As for the case study, the calculated stress ranges is related to a probability of exceedance of 10^{-8} and the cumulative damage has been calculated by two-slope S-N curves.

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7.3.1 Procedures Disregarding Parallel Stresses

Traditional procedures implemented in classification society guidelines do not consider effects of parallel stress on fatigue life of a welded joint.

The GL rules for the hot spot concept do not account for biaxial stress states. Instead, the stress component perpendicular to the expected crack front is considered to be fatigue relevant only. For the calculation of the permissible hot-spot stress range on both location the FAT 100 S-N curve has been selected as resistance reference. The stresses close to the hot-spot (nodal stresses) have been linearly extrapolated to the hot-spot and fatigue damage calculation has been carried out within the permissible stress range approach by considering a series of corrections. On the basis of the selected S-N curve, the permissible stress has been obtained, equal to 417.60 MPa, while calculated reference stress is equal to 17.28 MPa. Fatigue damage obtained by applying GL rules is almost zero as predicted damage to limit damage ratio equals 0.04. It should be noted that the GL quotes a more refined procedure in his guidelines. However, this is not reflected by the rules.

A tentative of fatigue checking has been also performed within the Common Structural Rule approaches for oil tanker and bulk carrier. According to CSR for bulk carriers, the notch stress approach has been applied based on maximum principal stresses within 45° normal to the weld, obtained by extrapolation of element stresses to the hot spot based on values which are read out at distances of $0.5t$ and $1.5t$ away from the hot spot location. The calculated reference stress range is 17.27 MPa. The S-N curve selected for the analysis is the so called "B-curve". The reference stress was modified according to a series of corrections. Calculation gives a fatigue damage of almost zero.

Fatigue predictions based on CSR for oil tankers are performed within the structural stress approach where the reference stress is the normal stress perpendicular to weld toe line $\sigma_\perp$. It is defined as the element stress read out at distance of $0.5t$ away from the hot spot location. The calculated reference stress range is 15.40 MPa and the calculation gives again a fatigue damage of almost zero.

According to KR rules, some fictitious beams without stiffness (with very low sectional properties) are to be put on the connection lines of shell elements in order to determine the surface stress distributions of the shell elements. However, in this study the shell element stresses are used. That is justified by the smooth stress field in front of the hot spot. Due to adopting the concept of getting stress from the fictitious beam at locations $0.5t$ and $1.5t$, the biaxial stress state at the toe of weld bead is implicitly not taken into account by KR rule. So, only the stress component normal to the weld line is used for assessing the fatigue life at the weld toe. Damage calculation is based on the $\sigma_\perp$ normal stress as reference stress (calculated stress range of 17.19 MPa), and refers to the so called "D-curve" capability curve. Resulting fatigue damage is zero.

The procedures applied above are all implemented in different ways, but all of them make use of approximately the same value for the reference stress and all deliver similar results.

BV and RINA implemented a fatigue checking procedure based on a reference stress selected as "the principal stresses at the hot spots which form the smallest angle with the crack rising surface" and within those procedures an allowance is applied for stresses parallel to weld axis. The same approach is proposed by DNV and discussed in the following section as the DNV maximum principal stress approach.

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7.3.2 A Procedure Accounting the Parallel Stresses: The DNV Approach

DNV rules (CN-307) prescribe to calculate the cumulative damage on the basis of the maximum principal stress approach. The fatigue relevant stress is the maximum principal stress at the hot spot in a sector of $\beta = \pm 45°$ normal to the weld line, calculated by linear extrapolation of the individual stress components of the plane stress tensor. Some reductions in the fatigue damage accumulation are allowed. Within that approach, also the case of a governing stress parallel to the weld toe line is considered. As the S-N curves for welded joints include the effect of the local weld notch, if the governing stress direction is parallel with the weld direction a stress reduction factor K_P should be used on the principal stress range before entering into the S-N curve. The stress reduction factor will depend on the quality of the weld and for a manual fillet or butt weld is $K_P = 0.90$.

As for the present case, a number of parameters have been set. The S-N curve is for welded joints in protected environment (FAT 90 S-N curve). The applied stress range is 35.54 MPa for the hot spot on deck and 63.10 MPa for the hot spot on longitudinal bulkhead. A result in terms of cumulated damage is almost zero for both hot spots when considering the principal stress. The cumulative damage value again is very low, but it turns out to be plausible if it is considered that reference stress range, i.e. the stress range acting perpendicular to weld toe line, is very low.

An optional method for deriving the hot spot stress is proposed in DNV rules and is intended to replace the traditional procedure on a voluntary basis. In this procedure effective hot spot (extrapolated from FE read out points) is calculated as the maximum of both principal stress reduced by K_P and an equivalent stress obtained by a formulation similar to that given in Eq. 6 where α is set to 0.81. Alternatively, the effective hot spot stress is calculated on the basis of the stress at the read out point $0.5t$ from the weld toe line without any extrapolation. In this case, the maximum value calculated as above is increased by 12 %. In both cases, the resulting effective stress is made to account for one of the two possible fatigue pattern shown in Figure 30.

Calculations within the enhanced DNV approach have been carried out under the same hypotheses made for the traditional approach and using a stress range of 237.72 MPa for hot spot on deck and 324.16 MPa for hot spot on longitudinal bulkhead. The cumulative damage calculated according to the alternative approach cumulative damages $D = 0.25$ for hot spot on deck and $D = 0.79$ for hot spot on longitudinal bulkhead. The role of parallel stress is here emphasized by utilizing the parallel principal stress as reference stress, which is in this case are the maximum principal stress. Expected fatigue cracking pattern is that of the right picture of Figure 30.

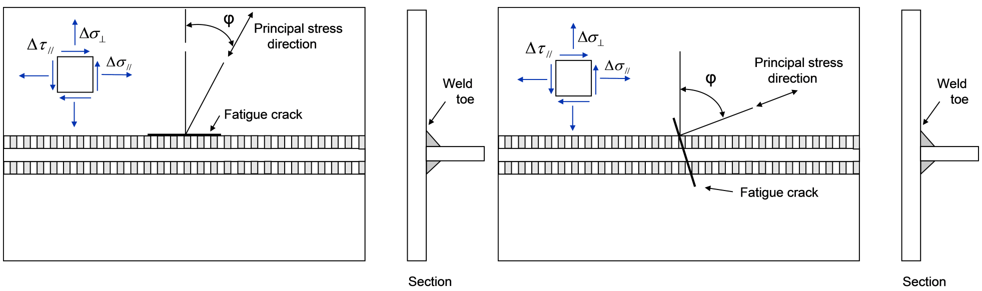

Figure 30: Fatigue cracking pattern when principal stress is approximately perpendicular or parallel to the weld toe line (DNV-CN30.7, 2010).

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7.3.3 IIW Guidelines for Stress Multiaxiality

Within the IIW approach, the fatigue analysis can be solved both by referring to maximum principal stresses and by summing up the effects of shear and normal stresses. In the case of variable amplitude loading, maximum principal stress is proposed as reference method, disregarding the direction of such stress. A limit Miner sum of $D = 0.5$ is recommended together with a modification of the shape of reference S-N curve. Alternatively, IIW suggests to calculate separately normal and shear stress cumulative damages and finally to verify their effects by a combination of both.Therefore, reference is made to a formulation as that given in Eq. 8, where the limit D_E is fixed equal to 1. The equivalent stress ranges are calculated utilizing the appropriate FAT classes for normal stress (parallel or perpendicular to weld toe line) and shear stress. The FAT class 90 is applied to normal stresses $\sigma_{||}$ and detail No. 323 "Continuous manual longitudinal fillet or butt weld". FAT class 100 is applied to shear stresses $\tau_{||}$. This procedure is only recommended for steel.

In the present study, a series of corrections have been applied on the stresses obtained by linear extrapolation between values corresponding to nodes $0.5t$ and $1.5t$ from the hot spot line. The Weibull shape factor and the total number of cycles are according to DNV guidelines. As for the method based on maximum principal stress, a stress range of $264.12\,MPa$ for the hot spot on deck and $360.46\,MPa$ for the hot spot on the longitudinal bulkhead has been referred to. The results in terms of cumulative damage are $D = 0.38$ for hot spot stress on deck and $D = 1.13$ for longitudinal bulkhead. The suggested damage limit is 0.5. In both cases hot spots appear about critical, and in the latter case the result shows that the fatigue resistance criterion is far from being satisfied.

The IIW-method on combination of the normal and shear stress cumulative damages deals with reference stresses $\sigma = 264.10\,MPa$ and $\tau = 34.52\,MPa$ for hot spot stress on deck, and $\sigma = 285.70\,MPa$ and $\tau = 129.11\,MPa$ for hot spot stress on longitudinal bulkhead. The combination of cumulative damages has been made basing on Eq. 8: for hot spot on deck and walland resulted in 0.52 and 0.65 respectively. Shear stress contributions are insignificant.

Importance of normal stresses parallel to the weld toe line is also highlight by Maddox (2010) who has proven by fatigue testing that fatigue loading beyond 60° from normal to a fillet weld can still produce weld toe failure. Laboratory tests were performed on plane specimens with attachments welded on different orientations from 0° to 90° with respect to the direction of loading (see Figure 31). A very good correlation of results in terms of fatigue capacity for different welded attachment orientations may be reached if the resultant stress range $\Delta\sigma_{res}$ is applied in the fatigue assessment. Maddox suggests for a nominal stress range $\Delta\sigma_0$ between $\theta = 45°$ and 90°, to assess the weld as longitudinal weld, with a fatigue crack controlled by the parallel stresses as shown in Figure 31. Consequently, a suggestion is made to base fatigue assessment on the resultant stress range $\Delta\sigma_{res} = \Delta\sigma_0 \sin\theta$, by using the normal stress $\sigma_{||}$ parallel to weld toe line. In the present study, the method based on Maddox's proposal an egligible shear contribution delivers the same results as the method based on combination of normal stress and shear stress damages.

7.4 Benchmark Study Conclusions

The present benchmark study is focused on the identification of the stresses to be used as the reference stresses to assess welds in case of stress biaxiality. Two cases have been studied: the hot spot location on deck where the welded joint is subject to

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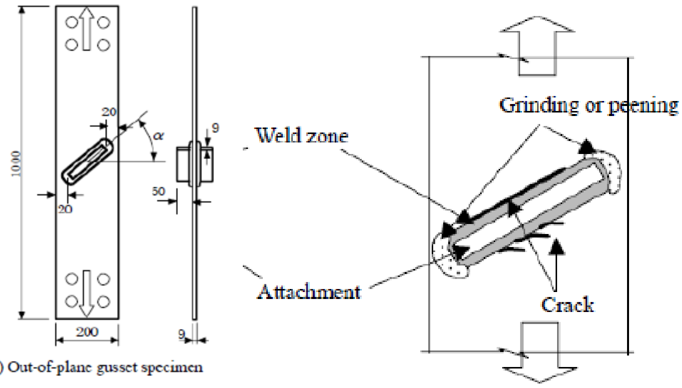

Figure 31: Test specimens and mode of fatigue cracking (Kim and Yamada, 2004 as cited in Maddox)

parallel loading (maximum principal stress parallel to weld toe line), and the hot spot on outer longitudinal bulkhead (maximum principal stress at about 30° to weld toe line). Different approaches have been implemented according to classification society's rules and scientific criteria. The different outcomes in terms of cumulated damage are summarized in Table 5 where σ_p stands for principal stress.

The low level of stresses normal to the fillet weld does not give advice on risk of fatigue cracking when traditional design rules are applied. In these cases the calculated fatigue damage D is about zero. The assessment is based on an approach of disregarding the effect of stresses parallel to the weld fillet. This approach, even if differently implemented by the classification societies, leads to a strong consistency between the calculated fatigue damages, but not with the scientific approaches.

A clear divergence from common indication of indefinite life is that given by DNV rules

Table 5: Fatigue damages resulting from different approaches

hot spot location: toe weld line on deck plate				
	method for fatigue checking	reference stress	limit value	D
GL	traditional method	$\sigma_\perp$	1.0	0.04
CSR(OT)	traditional method	max σ_P ($\beta = \pm$ 45°)	1.0	0.00
CSR(BK)	traditional method	$\sigma_\perp$	1.0	0.00
KR	traditional method	$\sigma_\perp$	1.0	0.00
DNV	traditional method	max σ_P ($\beta = \pm$ 45°)	1.0	0.00
DNV	enhanced method	max ($K_P\|\sigma_1\|$,$K_P\|\sigma_2\|$, σ_{res})	1.0	0.25
IIW	max principal stress based method	max ($\|\sigma_1\|$,$\|\sigma_2\|$)	0.5	0.38
IIW	combination of damages	max($\sigma_\perp$, $\sigma_{\sigma\|\|}$), $\tau_{\sigma\|\|}$	1.0	0.52
hot spot location: toe weld line on longitudinal bulkhead plate				
	method for fatigue checking	reference stress	limit value	D
CSR(OT)	traditional method	max σ_P ($\beta = \pm$ 45°)	1.0	0.03
CSR(BK)	traditional method	$\sigma_\perp$	1.0	0.00
IIW	max principal stress based method	max ($\|\sigma_1\|$,$\|\sigma_2\|$)	0.5	1.13
IIW	combination of damages	max($\sigma_\perp$, $\sigma_{\|\|}$), $\tau_{\|\|}$	1.0	0.65

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in the Classification Notes, N.30-7 "Fatigue Assessment of Ship Structures, 2010". The assess considers the stress biaxiality in front of the weld toe. The calculated fatigue capability is different from that previously outlined. Fatigue damage D comes out to be a small value but not trivial for the case when maximum principal stress is parallel to weld toe line, while for the case of maximum principal stress at about 30° to the weld toe line the fatigue damage shows a quite high value, but still not critical.

It is worth pointing out that some classification societies in their rules or guidelines also consider the case of a stress pattern predominantly parallel to weld line. Such procedures give results in accordance to those of the DNV enhanced method and IIW.

A different perspective is given by application of procedures based on the recommendations proposed by the research bodies, which define scientific criteria that, even if not ready to become standards, try to be more adherent to the physical phenomenon of stress multiaxiality in fatigue problems. A method reported by Hobbacher and Maddox have been applied. Resulting fatigue damages appear to deviate from each other, at least as for the quantitative evaluation of fatigue damage, but agree in pointing out the detail as a possible critical detail.

In conclusion, it is not clear if the examined structural detail is strong enough to withstand the load history considered, due to the considerable gap among the results obtained using different methods. Hence it is recommended that multiaxial fatigue is further investigated both by the researchers and the industry in order to come up with the best approach and harmonise the rules.

8 RECOMMENDED WORK FOR FURTHER RESEARCH

Recent years, the importance of crack initiation modelling for practical design gains increasing research interests. For high quality welds or treated welds, the crack-like weld defect does not exist, and thus, the initiation period can be a significant part of total fatigue life, Zhang and Maddox (2009) and Nykänen *et al.* (2009). In addition, short crack growth should also be investigated further in order to better line results from crack growth analysis and S-N fatigue analyses. The existing local concepts for welded joints does not capture crack initiation, since the S-N curves in the codes generally present the total fatigue life up to final failure, Radaj *et al.* (2009). Thus, a design approach, which can model separately the crack initiation and propagation, are required in the future. To obtain this, the existing theoretical models for crack initiation should be further developed. In the offshore community, fatigue evaluation is mostly based on nominal stress approach and S-N curves, however due to more complex and loaded geometries not directly covered by todays S-N curves, it is expected also better and more practicable local approaches in future will need to be developed.

Newbury (2010) presents a summary of the third ISOPE strain-based symposium focused on modelling, materials developments, and tensile and compressive strain capacity. It is concluded that there is no standardized method to characterize strain aging response, which is a critical factor affecting strain capacity. It is also stressed that there is no acceptable industry standard for SENT testing, which is a common test to assess the toughness level of pipelines. This issue ought to be further addressed in future work.

Design codes and associated design methods have a significant influence on the safety and integrity of the structure, and consequently, a fundamental impact on structural design. It is well-known that the uncertainties in any design method or design code are inherently high and not easy to quantify. It is recommended that the industry and

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the academia will continue to interact and work together towards the development of more robust methods and codes for design safer structures. Additionally, there is still a need for a common standard and guideline for designers working with fatigue and fracture. This was also pointed out by the members of the ISSC2009 report. However, it is anticipated that the outcome of the work carried out by the industry today will result in a common standard in the nearest future.

Effects of quality on fatigue strength was studied by Hobbacher and Kassner (2010), this is a very important topic which have high economical and safety related relevance; and it is recommended that this is a subject is further investigated.

Ageing of marine structures is a challenge for the society, as failures can have serious consequences such as loss of lives and significant pollutions. Thus, focus must be on the special issues of operating aged structures and the possible life extension of these. Offshore industry have come further in the development of analysis procedures of aged structures, and further research needs to address also safe operation of aged ships. Hence, a special working group within ISSC, Committee V.6 on Condition Assessment of Aged Ships, has been established for some time in order to investigate and focus on this topic more in detail.

New materials are continuously developed, and especially beneficial properties with respect to e.g. fatigue resistance and higher strength have been focus areas for steels. However, when used in large-scale structures, or cold climates, the systems perspective must be considered. Functionality and reliability of structural designs will probably change todays design and offer new opportunities, cf. goal-based standards principles. A growing trend to explore the possibility to use composite materials in ships will require that we in the near future better understand and can model these materials properties, failure modes and fatigue resistance.

9 CONCLUSIONS

The committee has reviewed recent works concerning the topics fatigue and fracture identified by the committee mandate. This report describes the results of a literature survey of more than 200 references, in addition to a benchmark addressing mutiaxial fatigue study carried out by the members. Based on the committee member's knowledge and interest, this report has focused on unstable crack propagation, multiaxial fatigue, new materials, damage control and risk-based assessment, and update of latest changes in design methods for ships and offshore structures. Topics discussed in details by the previous committee III.2 (2009) have only briefly been discussed in this report and it can be recommended to see the two reports in conjunction to each other to get a more detailed picture of the resent research within fatigue and fracture.

Section 2 presents a brief overview of the recent developments in fatigue assessment methods with focus on the initiation and propagation phase. The chapter has had special attention to multiaxial fatigue analysis procedures, which is still a quite new approach used in the oil and gas industry and maritime sector, still practical fatigue analyses is mostly based on first principal stresses.

Due to the growing interest of exploring oil and gas in the Arctic, in addition to shipping in harsh environment and the wish to build larger vessels, research within brittle fracture and unstable crack propagation have seen a high focus of research lately. Section 3 discusses some of the resent research within these topics with focus on methods for the analysis and experimental measurements of nucleation and propagation of brittle fracture with an example of how brittle crack propagation can be prevented in container ships.

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New developments and new findings on most commonly used materials seen in the offshore and shipping industry have been discussed in Section 4. Emphasis has been put on high strength steel, new steel with improved crack growth properties, materials for cold climate, honeycomb structures, etc. Material research has a high focus within the industry and it is important that findings beneficial for the marine structural design are incorporated into rules and guidelines.

In Section 5, methods suitable for damage control and risk-based assessment are reviewed, considering factors such as effect of workmanship; internal defects, welding procedure, etc. have been discussed. Attention is paid to the suitability and uncertainty of physical models, uncertainty in fatigue assessment diagrams and fatigue life assessments. In addition ageing of structures which is a challenge for the society, have been addressed with focus on special issues regarding operating of aged structures and the possible life extension of these. Both the Petroleum Safety Authority (PSA) in Norway and the HSE offshore division in UK have worked intensively on defining requirements for safe operation of offshore structures extending their original design lives, however these topics will most likely continuing to have a high priority within the marine structures communities.

Section 6 deals with fatigue design methods for ship and offshore structures and a review of update and comparison of design methods for ships and offshore structures, such as the Common Structural Rules (CSR) for oil tankers and bulk carriers, the Harmonized Common Structural Rules (CSR-H) for oil tankers and bulk carriers, rules for offshore installations and Arctic design codes have been addressed. The industry is continually updating their guidelines and recommendations in order to include the latest research.

Multiaxial fatigue procedures are not used to a large extent in ship and offshore design, despite that most structures are subjected to multi-axial stresses most design rules only consider the first principal stress. A benchmark study was presented in Section 7 focusing on multiaxial fatigue. The intention of the study is to show how fatigue assessment of welded structures are treated differently when unconventional loading pattern is to be applied on a simple welded joint. The results showed a discrepancy of obtained lives between the different rules and it is recommended that further work is put into the topics of multiaxial fatigue and the practical pint of it.

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