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WIND INDUCED RESONANT CROSS FLOW VIBRATIONS ON NORWEGIAN OFFSHORE FLARE BOOMS

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ABSTRACT

Vibrations and fatigue cracking have occurred, for some years, in flare boom structures on a number of platforms on the Norwegian continental shelf. In view of this, the objective of this paper is to:

- Describe the vibrations and its characteristics on Statfjord A and Heimdal which had the largest vibrations. The vibrations can not be predicted by vortex induced vibrations of individual members but could be caused by wake effects, local frame vibrations or global dynamic effects.
- Review the evaluation of cracks in other flare boom structures. The work has been based on DIN 4133 and includes vibrations of both individual members and frames.
- Review the design practise and operational experience with design parameters especially measurements of damping on individual tubular members and observed vibrations of individual members and frames. Possible limitations of the DIN standard will be discussed.
- A promising device for reducing the vibrations is described.

The problems have mainly been caused by the methods used in design, however, evidence of welding defects initiating fatigue cracks has also been found. A review of the existing methods has led to the recommendation of using the German DIN 4133 code for design purposes. This code is to a large extent similar to the proposed Eurocode-1. A design procedure based on DIN 4133 is proposed including additional parametric boundaries for avoiding resonant vibration.

INTRODUCTION

In 1978 and 1984 severe vibrations was observed in the flare boom structures on Statfjord A and Heimdal. The maximum oscillation amplitude was estimated to 50 mm. Since then, we have not experienced such large vibrations, but other and smaller vibrations resulting in fatigue cracking.

The vibrations on Statfjord A and Heimdal were observed from the deck of the platforms. Inspecting the flare booms more closely was difficult due to the complications with building of scaffolding. In recent years, mountain climbing techniques have been used for inspections of the flare booms. Inspectors with mountain climbing equipment and sufficient training are climbing in the flare boom structures searching for cracks. Using this method a much more simple access to the structures has been achieved. These close inspections did quickly reveal that cracking represented a severe problem for a number of the flare boom structures. Especially many cracks have been found on Statfjord B and C, Gullfaks B, Vallhall PCP and Odin.

WIND CLIMATE

The wind climate on the Norwegian continental shelf is characterised by the large variability associated with low pressure systems and ridges embedded in the westerly zonal flow along the North-Atlantic polar front. The long term distribution of the mean wind speed is reasonably well given by a 2-parameter Weibull distribution.

As an example, the long-term cumulative distribution and the annual wind frequency distribution for the 1 hour mean wind speed at 10 m above the mean sea level for the northern North Sea area are given in Fig. 12 and Fig. 13 respectively, Andersen and al. (1987). The yearly mean wind speed is seen to be about 9 m/s and the 100-year value is about 41 m/s. The prevailing wind direction is N and S with a considerable wind load also from W. Flare boom structures are usually placed on the east side of the platforms thus mostly exposed to wind from N and S.

OBSERVED VIBRATIONS AND ANALYSIS REVIEW

Statfjord A

The Statfjord A platform was installed in 1977. The layout of the flare boom structures are principally equal for the Statfjord platforms as shown in Fig.1 presenting Statfjord B/C. On Statfjord A the ties are fixed to a module. Some geometry parameters of vibrating members are given in Table 1.

On September 17, 1978 the main ties of the flare boom at Statfjord A were subjected to severe vibrations. A similar vibration occurred on October 2 and a somewhat different vibration on October 19. Mobil (1979), give some conflicting information as to the observations and evaluations. Following the incidents a series of communication and meetings took place and a range of possible excitation sources were proposed and investigated. Among these were vortex shedding from the flare line, flutter of the main ties and acoustic resonance in the flare line (organ pipe effect) and mechanical excitation sources on the platform. The vibrations were generally coupled with airborne and mechanical noise. The first site report stated that the vibrations seemed to be caused by vortex shedding from the tubular main chords and that the problem might be resolved by installation of vortex spirals. This was in fact one of the procedures selected for action and a rope was fitted, as a preliminary precaution, in the autumn with reportedly a good effect.. In the following summer, the ropes were replaced by permanent braces clamped to the structure and thereby reducing the span width of the chord members in the vertical plane of the ties.

With some interpretation of the report the following observations are made. During a period of maximum vibration of the main ties on Sept. 17, the wind speed was recorded at V(10s)=21 m/s as 10 s average (40 knots) and direction 170 degs related to true north, platform north being at 23.6 degs. The main ties vibrated in a vertical plane with a frequency of about 5 Hz which is close to the estimated natural frequency of the tie. A first mode vibration of the tie was visually observed and possibly with a mode other than the fundamental mode. Maximum amplitude of vibration was estimated to 50 mm (50 to 100 mm, peak to peak). The vibrations started with excitation of central lower chord members. On the incident reported on Oct. 19 the wind was recorded at about V(10s) = 22 m/s (40-43 knots as 1 min average) from 200 degs. The south main tie vibrated with a frequency of about 19 Hz in a vertical plane. The vibration started with excitation of the smaller tubular bracing's in the top panel of the tie, then engaging the chord members. The amplitude of vibration was estimated to 25 mm. The north main tie vibrated in a similar manner with a frequency of about 20 Hz.

Mobil (1979) conclude that the natural frequency of the chord members of the main ties was very close to their vortex shedding frequency, hence a state of resonance. The conclusion was based upon recordings and observations during the incident of Oct. 19 by DNV inspectors. This is in some controversy with observations reported by eye witnesses of a more global tie oscillation. There is also some deviating information about the wind direction during the first two periods, being NW-W. However, this is not substantiated by the observed structural response.

In connection with this paper the critical velocities for some members have been checked, ref. Table 1. The local vibration mode of the chord members is found to have a natural frequency of about 19 Hz assuming end fixities of 10 %. This results in a critical velocity of about 21 m/s which is within the range of recorded wind speeds during observed vibrations. The observed vibration amplitudes, however, is not substantiated with calculations based on vortex induced vibrations of single members, i.e. approx. 5 mm. The question remains whether the local vibration could have been overlaid or possibly initiated a global dynamic vibration. It is noted that the natural vibration of the chord is nearly in phase with that of the tie, so a local vortex vibration might have initiated a global vibration.

NDT inspections, performed in connection with the reported vibrations, did not reveal any cracks and no further vibration problems have been encountered. The flareboom structure has been subjected to inspection during maintenance periods without discovering defaults, last inspection was performed in 1994. The conclusion that the vibrations were caused by vortex shedding from the chords of the main ties is thereby substantiated.

Statfjord B/C

The Statfjord B platform was installed in 1982 and Statfjord C in 1984. The layout of the flarebooms are shown in Fig. 1 and some geometry parameters of the vibrating members are given in Table 1.

During the period 1990-94 a number of 22 and 30 joints with cracks have been found on the Statfjord B and C flareboom wind struts respectively. There is a difference in the failure pattern on the two platforms. A general pattern is cracks in the x-joints between bracings and cross members in the upper and lower panels. On Statfjord B there is additional cracking in joints at the chords. A metallurgical investigation conclude with fatigue rupture, one earlier testing from Statfjord C indicate initiation from welding defects.

The flareboom wind struts were analysed by Veritec, Askheim and al.(1993), with the conclusion that there is no reason to believe that the cracks are caused by static or traditional (global) dynamic loading. With a high degree of confidence, it is further concluded that the cracks are caused by cross wind vortex induced vibrations. A good correlation between calculations and observations has been established. The analysis showed possible vibrations of single members and also a vibration mode with an internal grid of members (frame) as a possibility. The model with a vortex induced frame vibration was used to substantiate observed cracking in the cross members supporting the diagonal bracings (x-panel). The joints have high calculated stress concentration factors, above 4, which contribute significantly to the fatigue effect.

The damages have been repaired and new cracks have been located outside the repaired areas. In June 1993 the upper and lower diagonals of the wind struts were fitted with vortex mitigation "sleeves" as presented in Ch. 5.

Gullfaks B

The Gullfaks B platform was installed in 1987. The layout of the flare boom is shown in Fig 2 and some geometry parameters are given in Table 1.

During inspections in 1992, about 12 joints located in the wind-struts were observed with cracks. A metallurgical investigation showed a type of low stress fatigue rupture and corrosion attack indicating a slowly growing process of cracking. The flareboom was analysed by Aker Engineering, Lefranc and Markhus (1992), concluding that the damages were caused by vortex induced cross-wind vibrations of single elements. A global dynamic analysis gave a maximum deflection of the wind-strut of approx. 30 mm but with no fatigue effect. The analysis showed no coupling between vibration of local members and global vibration modes. A local coupling between two of the members with observed cracks was shown resulting from natural frequencies in the relation 1 : 2.

The cracks have been repaired and the strut chords have been put under surveillance for penetrating cracks by means of an internal overpressure with inert-gas. In june 1993 the diagonal members of the wind-struts were fitted with vortex mitigation "sleeves", as presented in Ch. 5.

Heimdal

The Heimdal Platform was installed in 1984. The layout of the flareboom is shown in Fig. 3 and geometry parameters of some vibrating members are given in Table 1.

The flare boom experienced in 1984 and 1985 a number of vibrations as individual elements and as frames. Several frames in the structure vibrated with 2 and 3 Hz. The vibration amplitudes were reported to be from "small" to "minimum 50 mm". The largest vibrations of the individual members were about 40 mm. There was reports on both continuous vibrations and vibrations occurring more at random . No overall vibration of the flare boom was reported.

The analyses, Fines et al.(1985), showed a substantial difference between the behaviour of x-braces which were braced out of plane and k-braces which were unbraced. The x-brace elements acted as single elements, while the k-braces vibrated as frames. The k-braces had several modes of vibration at closely spaced natural frequencies and responded in a complex manner to vortex shedding The calculated natural frequencies were close to the measurements. The measurements showed significant response at more than one frequency. The vibrations are, most probably, found to be caused by wake interactions with the chord members. Fig. 4 presents the reported vibrations of individual members at Heimdal as a function of reduced velocity. The figure is based on clamped end fixity of the elements, a is the vibration amplitude and D is the diameter of the vibrating element. An envelope of these values are close to the results reported by Moe, Steen and Domben (1993). as given in Fig. 5. The figure presents plots of maximum single cylinder vibration amplitudes (dashed line) and vibration amplitudes for the same cylinder in a wind tunnel test in the wake of an 80% larger cylinder, both as a function of the reduced velocity. It is noted that the wind velocity is increased to a level where the wake frequency from the large diameter cylinder is near the natural frequency of the smaller cylinder.

Using a Strouhal's number of 0.2 a cylinder with a diameter of about 1.2-2.5 m is required to get wake frequencies of 2-3 Hz at the measured wind velocities, corresponding to observed frame vibrations. There is no members in the boom having this diameter. Using a reduced velocity of about 8, as found for individual members, will require a cylinder diameter in the order of magnitude 0.7 - 1.6 m, 0.7 m is close to the largest diameter tube in the flare boom. No vibrations of this tube have been reported. It should be fair to say that no good phenomenological explanation has been given of the frame vibrations in the documentation.

Analysis of some elements, based on DIN 4133, indicate fatigue from cross flow vibrations of single elements, ref. Table 1. Considering a vibrating diagonal member (290-314) with clamped end fixity exposed to the recorded wind, taken as a 30 s mean wind with its component perpendicular to the member axis. This results in a reduced velocity of 6.2 which is within the lock-in range for vibration as a single member and also consistent with the maximum single cylinder vibration shown in Fig, 5; indicating no wake effects. The observed vibration amplitudes are, however, not substantiated by calculations based on the DIN-code. Related to the frame vibrations the possibility exist for a lock-in effect or excitation by a frame member in a phase frequency.

Odin

The Odin Platform was installed in 1983. The layout of the flareboom is shown in Fig. 6 and geometry parameters of some vibrating members are given in Table 1. Cracks have been found several places in the flare boom. No vibration have visually or instrumentally been observed.

Analysis of some elements, based on DIN 4133, indicate that cross flow vibrations are a possible reason for the cracks, ref. Table 1. A possible exception is element 2, which have a high critical velocity. However, compensating for a critical flow with a value of Strohals's number of say 0.24 resuls in a critical velocity of 52 m/s and a fatigue life of 19 years.

Valhall PCP

The Valhall PCP Platform was installed in 1981. The layout of the flare boom is shown in Fig.7 and geometry parameters of some vibrating members are given in Table 1. Cracks have been found several places in the flare boom, no vibration have visually or instrumentally been observed. For some cracks, slag inclusion is the most likely background for the cracks.

Analysis of some elements, based on DIN 4133, indicate that cross flow vibrations of single members are the most likely reason for the cracks. An exception is element 3, which might be engaged in a frame vibration, ref. Table 1.

DESIGN BASIS

Design practice review

Design practice has been based on the notion that if the vibration amplitudes was kept low, ($a/D<0.02$), the induced fluctuating force would be randomly distributed over a broad band of frequencies. The vortex induced force would thus vary randomly along the length of the member. For larger amplitudes ($a/D>0.04$), the induced force would be almost periodically and with the energy concentrated to a relatively narrow band of frequencies. For such a case the vibration of the member would force the vortex shedding frequency to "lock-on" to the natural frequencies of the member, i.e. a state of resonant vibration. A broad band response would therefore be favourable and used as a criterion for avoiding adverse effects of vortex induced vibrations. The means to control this were to limit the reduced velocity value as a function of the flow regime (Reynolds number) and with additional requirements to the so-called stability parameter (Scruton's number), thereby controlling the amplitude of vibration, if the reduced velocity requirement could not be met. The effect of vortex shedding was considered negligible for values of Scruton's number larger than a certain value. In Statoil (1985) the limiting Scruton's number was specified to Sc= 16. The formula used for calculation of a maximum amplitude was sensitive to the stability parameter and thereby damping. A rather high value of damping was assumed , 0.5 % of critical. Ref. Statoil (1985), DNV (1991) and ESDU (1985).

After experiencing vibration problems at Heimdal in 1984-85, the Statoil design specification was revised in 1990 shortly followed

by a revised NPD guideline. The vibration amplitude was made a function of the reduced velocity level for peak vibration and the stability parameter used without any limitation, Moe (1989). The specification was considered conservative for critical and postcritical flow and found not to yield "robust" and economical structures in that otherwise required aspect ratios were excluded.

The procedures in general are found to overestimate the effect of damping and underestimate the fatigue effect due to high natural frequencies of the structural members, i.e. number of load-cycles. Combined with a high probability of occurrence of a range of critical velocities, as seen in the long term wind distribution, a number of cracks have been observed.

The DIN 4133 standard

In search for a design procedure yielding economical and "robust" structures consistent with observed behaviour, the DIN 4133 standard has been adopted in the present NPD guidelines, NPD (1992). It is used with some modifications as project specification in Statoil. The DIN-standard is less sensitive to damping and Scruton's number is introduced with no limitations. Maximum vibration amplitudes are calculated as a function of the vibration mode in addition to the aero-elastic response and damping. A reduced lift force and cut-off level for high wind velocities are introduced. Fatigue is calculated based on a defined wind velocity probability distribution

With reference to DIN 4133, offshore applications have raised some questions and uncertainties:

- Is the calculated amplitudes peak or RMS-values. Peak values are generally assumed.
- Which wind gust period should be used.
- Should the basic lift force coefficient be reviewed.
- Should the scaling of the coefficient for the lift force be adjusted.
- Should the correlation length factor be reviewed.
- Should the equation for calculation of the number of stress cycles be adjusted.

It is generally felt that the wind characteristics i.e. velocity distribution in time and space and level of turbulence should be related to offshore conditions based on long-term statistics. The level of turbulence in the up-stream wind is believed to determine a cut-off level for shedding of regular vortices and thereby vibration. The high natural frequencies involved are expected to respond to relatively short wind averaging periods.

As a design basis for the Sleipner A development the DIN 4133 standard with the following adjustments was adopted:

- The design wind velocity is generally based on 30 seconds mean gust during a 100 year return period taken from a long-term wind distribution.
- The dynamic lift force coefficient is scaled to zero for critical wind velocities above 40 m/s (as in DIN 4133) but the limit is adjusted for actual heights above 10 m from MSL.
- Wake interaction shall be considered by distances below 15 times the diameter of the smaller tube if the wake frequency appears to be at the same magnitude as the natural frequency of the smaller (down-stream) tube.
- The number of stress cycles is calculated on the basis of a long-term wind distribution for offshore conditions.
- Fatigue is calculated according to NS 3472 / T-curve for joints, a correction for small tube thickness is not made.

A relative low value of damping is assumed, 0.15 % of critical.

Alternative parameters are seen as part of a Norwegian design practice:

- For scaling of the dynamic lift force coefficient it is used a maximum wind velocity based on a long-term distribution with 30 s mean and 100 year return period adjusted for the actual height, Askheim and al. (1993).
- Hybrids between parameters related to the "broad- band" theory, i.e. limitations of Re and Sc, and the wind velocity cut-off level introduced with DIN 4133 have been used.

As demonstrated in Table 1, an analysis based on DIN 4133 predicts reasonably well a fatigue response. It is following the SLA-specification, including a tentative wind distribution and height level, and is indicative only. It is noted that fatigue is identified analytically and by observation for high Scruton's numbers (Sc=34) and for critical Reynold's numbers (Re $>2\times10^5$) assuming Strouhal's number St=0.2. Furher, evidence of cracking is observed in elements with critical velocities up to 65 m/s. Using a higher Strouhal's number in the critical and postcritical range, say St=0.24, results in reduced critical velocities (55 m/s) and reduced fatigue lives.

Parameters

Scruton's number. Some design rules state that the vibrations will stop when the Scruton number exceeds a certain number. DNV(1977) use 16 and DNV (1991) use 25. In Fig. 8, the number of members with cracks or vibration problems as a function of the Scruton number for all the Norwegian flare booms are given. For frames, the properties of the cracked member are used to calculate the Scruton number. Damping is set equal to 0.15 % of critical. Most of the problems have occurred on members with Scruton number less than 25. Even though the large vibrations on Statfjord A were on members having a Scruton number of 28 and several members with fatigue cracks on Gullfaks B having 34. No obvious limit is found for Sc in the members having cross flow vibrations.

Reduced velocity. Some codes, e.g. BS 8100 and Eurocode-1 (draft), prescribe limitations of the reduced velocity or, equally, the critical velocity in relation to the wind velocity. The limiting values will depend on the basis of selected wind parameters and flow regime, i.e. Strouhal's number. A reduced velocity of 4 using a wind velocity averaged over 10 min. corresponds to a reduced velocity of about 4.6 using a wind velocity averaged over 30 s. Referring to the observed vibrations on Statfjord A, BS 8100 is found to be non-conservative and Eurocode-1 conservative.

As experienced, e.g. on Oseberg and Troll A, a design based on a criterion aiming at completely avoiding cross flow vibrations may result in expensive solutions.

The Heimdal offshore measurements (Fines, 1985) and the model testing (Moe et al, 1993) have demonstrated that wake effects can move the limits were the cross flow vibration occur to higher reduced velocity numbers. The Statoil specification for Sleipner require that wake interference should be evaluated if a member is closer than 15 diameters. How to handle wake effects in design is not specified in any design standard.

Damping. Measurements of damping have been performed on several welded tubular steel structures. The results have a

tremendous scatter, as demonstrated in Fig. 9. Percentage of the critical damping as a function of the L/D ratio are given. Squares refers to Koch (1989) and the measurements on Bullwickle, "H" to Heimdal as reported in Fines et al (1985). "NR" refers to North Rankine, "M" to Murchison and "C" to a Chevron jacket, all of these data are taken from Rudge (1990).

The large scatter may be one of the reasons why many members according to the calculations have a very low fatigue life, but without any sign of cracking.

Displacements. Using the design concept in DIN 4133, the predictability of the low amplitude cracking in several platforms have been good. Even though the observed vibrations at Statfjord A and Heimdal indicate vibration amplitudes significantly higher than found using DIN 4133. Values of the maximum displacements of individual members divided by the diameter of at least 0.11 have been found on Statfjord A and 0.13 on Heimdal.

The maximum amplitudes at Statfjord A and Heimdal represent some 5 standard deviations of the values obtained using the DIN standard as giving RMS values. Bell and Morgan (1988) reports from the measurements at Murchison that typical values from the measurements were maximum values of about 5 standard deviations. However, our conclusion is that DIN 4133 give peak values and that the large displacements are caused by other effects, i.e. global vibrations or possibly wake effects.

Frame vibrations. On Statfjord A, B and C and Heimdal cross flow vibrations of frames have been visually observed or found as the reason for cracking in tubular joints. On Statfjord A two complex ties vibrated. On Statfjord B vibrations occurred in 15 "x-panels". On Heimdal the vibrations occurred in several "k-panels". According to Grundmeier et al (1989) vibration of and damage have been observed on 3 "x-panels" on jackets after trans-oceanic tow. How to handle such vibrations is not covered in any offshore design code.

In their panel analysis, based on eigenvalue analysis, Askheim et al (1993) assumed that the vortex shedding acted on one diagonal where the members meet each other. They argue that it is not likely that the force will act in phase on several members at the same time. They use one of the cylindrical members as an excitation source.

A possible procedure might be to analyse members for loads from vibrating members connected to them, assuming a correlation between attached members.

VIBRATION REDUCING MEASURES

Screening study

In the engineering study following the observation of cracks on the Gullfaks B flare boom structure a number of measures (some 25) were examined, falling into three main categories:

- Increase the stiffness in order to avoid critical wind velocities
- Disturb the shedding of vortices and thereby prevent local vibration
- Increase damping in order to reduce amplitude of vibrations.

The following measures were considered to be the most viable ones:

- Redesign and construction of new struts.
- Install a grid panel.
- Vary the diameter along the member axis
- Apply mechanical dampers
- Install spoilers on the vibrating members.

Vortex mitigation sleeves

The concept of vortex mitigation devices offering a variation of the member diameter was selected for a closer study. Based on reported effect of lightweight sleeves mounted on tubular structural members, Rudge and al. (1992), a wind tunnel test was performed at Sintef / NTH, Strømmen and Hjorth-Hansen (1993). The result was quite promising and sleeves have been installed on the Gullfaks B flareboom structure and also on the Statfjord B and C flareboom structures.

The sleeves offered some obvious advantages:

- Easy to install by climbers without any need for scaffolding.
- A fully reversal procedure, the sleeves could be removed without leaving any marks on the structure.

Besides, the sleeves could easily be manufactured, by the company, within the available time.

Model testing. In order to arrive at a possible solution to be adopted during a forthcoming maintenance period on the platform the testing was limited to:

- Identify possible resonant vortex shedding on a typical single structural member
- Investigate the effect of additional non-structural short compact (but light-weight) sleeves.

In their report Rudge and al. (1992) indicated, but not precisely quantified, that for a certain configuration with short sleeves with an outer diameter of about 1.5 times that of the tube itself, spanwise located at the 1/3- and 2/3-points, the resonant vortex shedding response was "strongly" reduced . This was used as a basis for the testing which gave the following result:.

The dynamic response behaviour of the single test-cylinder is caused by a narrow-band vortex shedding process, ref. Fig.10. The quasi-resonant response peak was found to occur at a reduced velocity range of 4 to 5.5. It is noted, however, that the recorded damping in the testrig was approx. 0.6 % of critical. This is considerably higher than can be expected in a welded latticed structure, say 0.2 %, and a wider (but unknown) lock-in range should be expected. For the "naked" cylinder the largest resonant response peak was found to have a non-dimensional RMS-value $a/D = 1.1 \bullet 10^{-3}$. From one representative recorded time serie in the vicinity of the largest RMS response peak the ratio between the maximum displacement amplitude and the corresponding RMS-value was about 2.5; indicating a maximum peak amplitude of about a/D=0.003.

All the tested mitigation devices reduce the resonant vortex shedding excitation significantly. The most effective was the configuration with two sleeves spanwise located at the 1/3 and 2/3-points. The maximum RMS response value was reduced to approximately one tenth of the value obtained for the "naked" cylinder alone.

The spectral density of the vertical displacement is shown in Fig. 11. It is seen that the sleeves are moving energy towards lower reduced frequencies (Strouhal's numbers). The main resonant peak, however, is well defined and occurs at a Strouhal's number

slightly above 0.2. Secondary peaks are believed to be connected with disturbances in the test set-up. One can speculate that the effect of the sleeves is caused by shifts in the vortex shedding frequencies, optimally counteracting each other, along the cylinder. Turbulence created from the edges of the sleeves might also have a significant contribution. Indications was obtained that the drag of the tested configurations with mitigation devices was only insignificantly different from the drag on the "naked" cylinder.

In-place experience. After one year of operation, no further cracking has been observed on GFB and STB, on STC one crack is observed. Analytically, the effect of the sleeves are higher on GFB than STB/C for fatigue sensitive members. The winter season 1993/94 had a rather mild wind climate but the effect of the sleeves looks promising.

CONCLUSIONS

The design against crosswind vibration may be based on the DIN 4133 standard. The wind data should be based on a long term distribution for offshore conditions. Alternatively, for practical design purposes if not cost-effective, a critical velocity range or a reduced velocity range for avoiding vortex induced vibrations may be given.

The design basis is connected with some uncertainty and should be calibrated versus results from model testing or full scale observations, especially:

- Strouhal's number as a function of the flow regime and lock-in range for resonant vibration.
- The design wind velocity and the effect of up-stream turbulence on the cut-off level for vortex induced vibrations.
- Wind directions not perpendicular to the member axis and the effect of local disturbances.

Criteria for avoiding resonant vibration may be given as limitations of the reduced velocity or, alternatively, as the critical velocity related to a design wind velocity. The criteria have to be related to a wind averaging period and probability of occurrence and the relevant flow regime.

Vibrations in the wake of vortices from structures up-stream should be considered. Vortex induced vibrations of parts of a structure, referred to as frames, may also be relevant. Necessary robustness of the structure will further limit the T/D ratio. i.e. requirements related to local buckling and welding quality.

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FIGURES AND TABLES

Figures

Tables

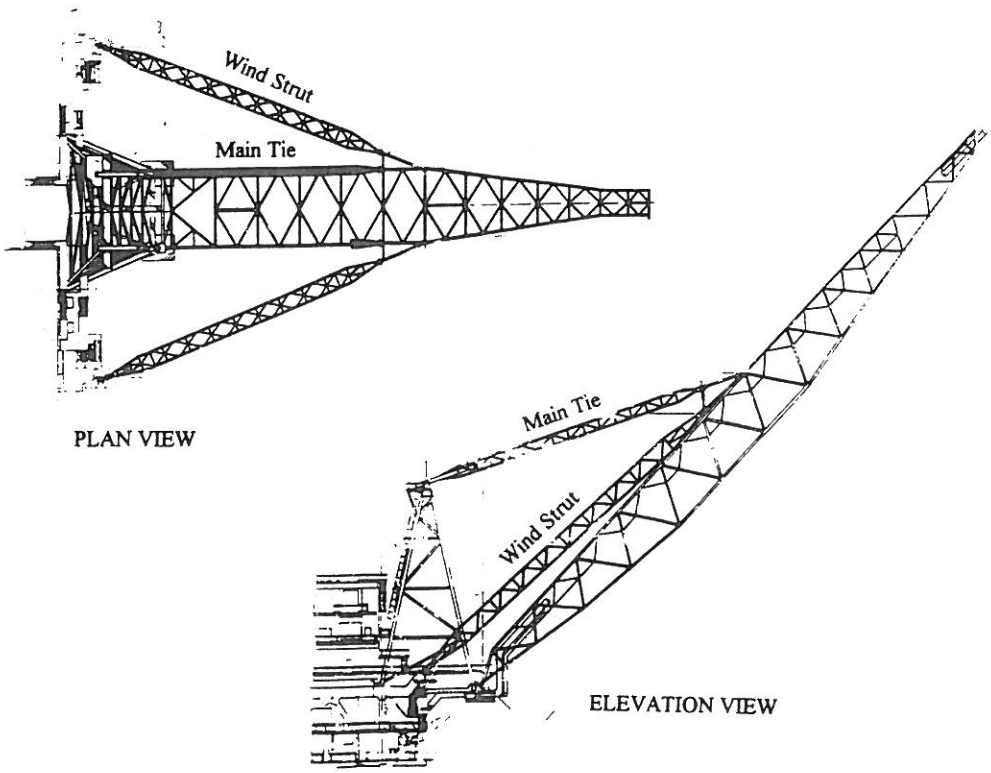

Figure 1. Statfjord B/C Platform. Flare Boom Layout.

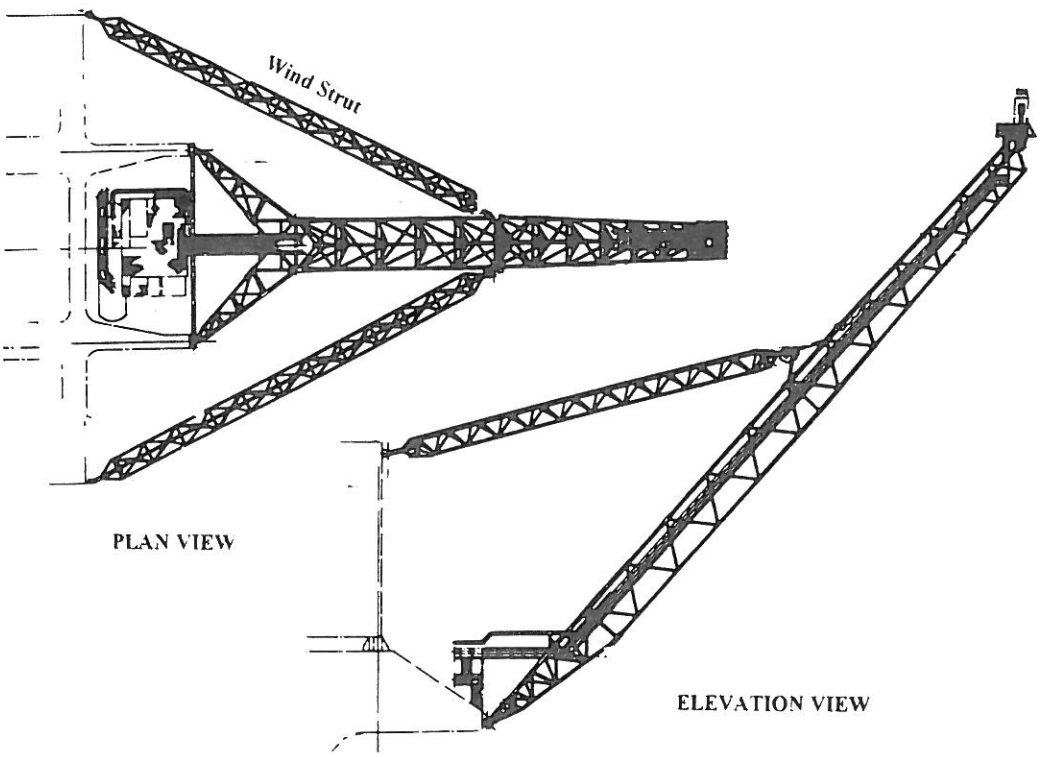

Figure 2. Gullfaks B Platform. Flare Boom Layout.

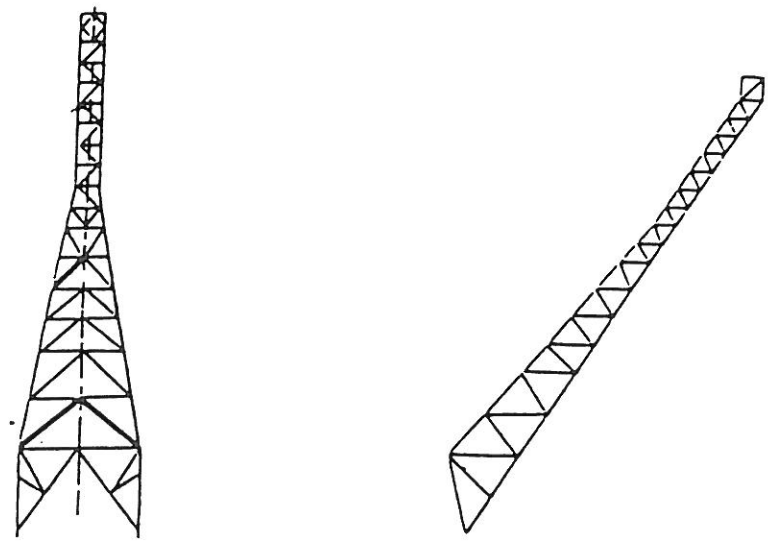

Figure 3. Heimdal Platform. Flare Boom Layout.

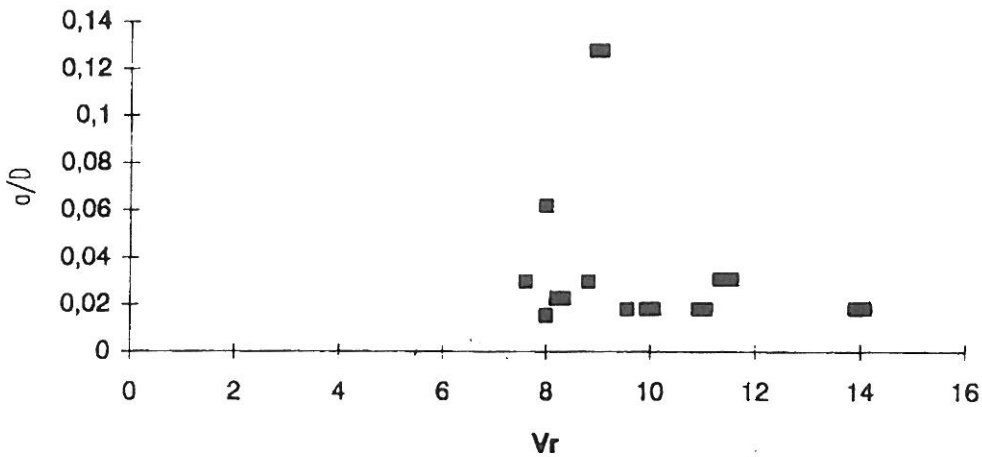

Figure 4. Heimdal Platform. Observed Cross Flow Vibrations.

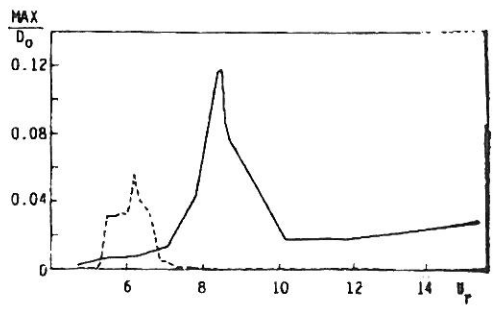

Figure 5. Model Testing / Heimdal. Cross Flow Vibrations in Wake

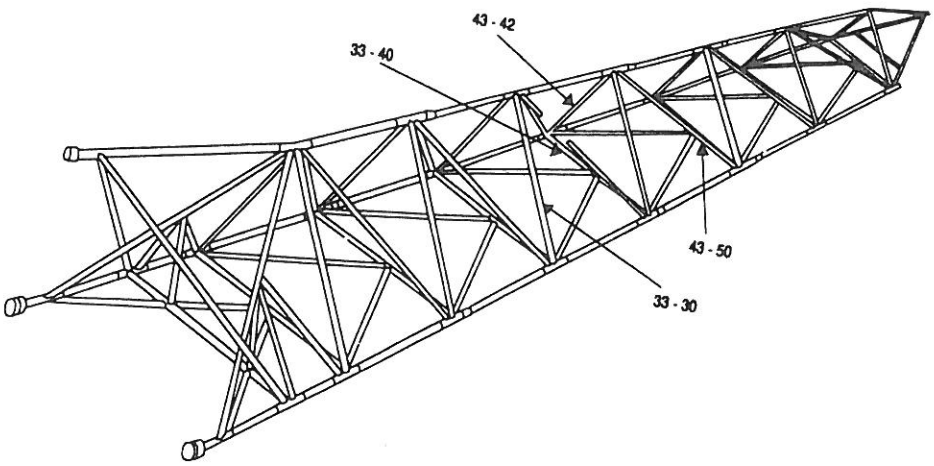

Figure 6. Odin Platform. Flare Boom Layout.

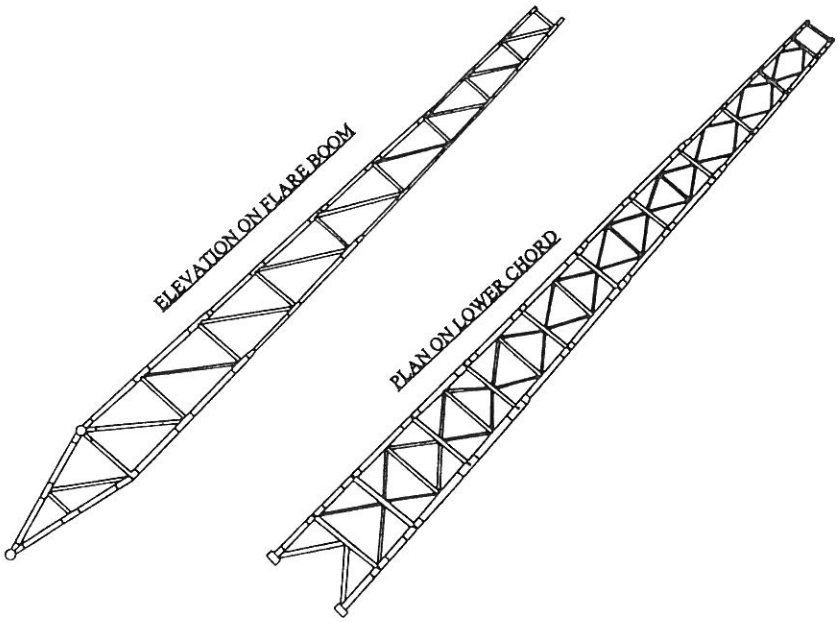

Figure 7. Valhall Platform. Flare Boom Layout

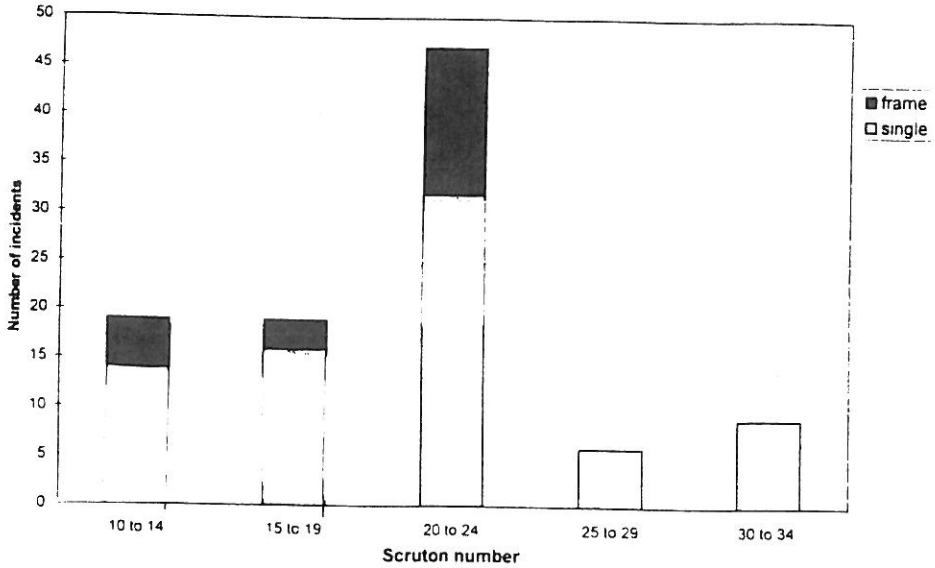

Figure 8. Observed Vibrations as Function of Scruton's Number.

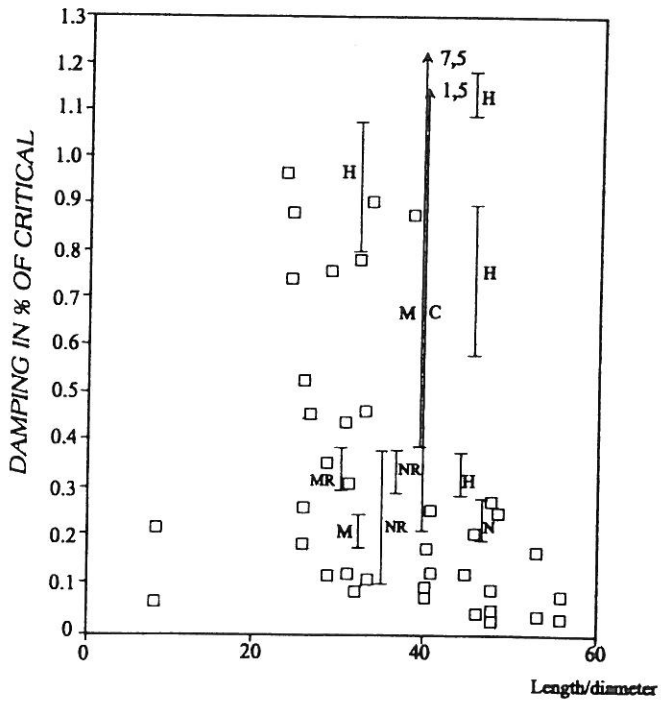

Figure 9. Observed Damping as Function of the L/D Ratio.

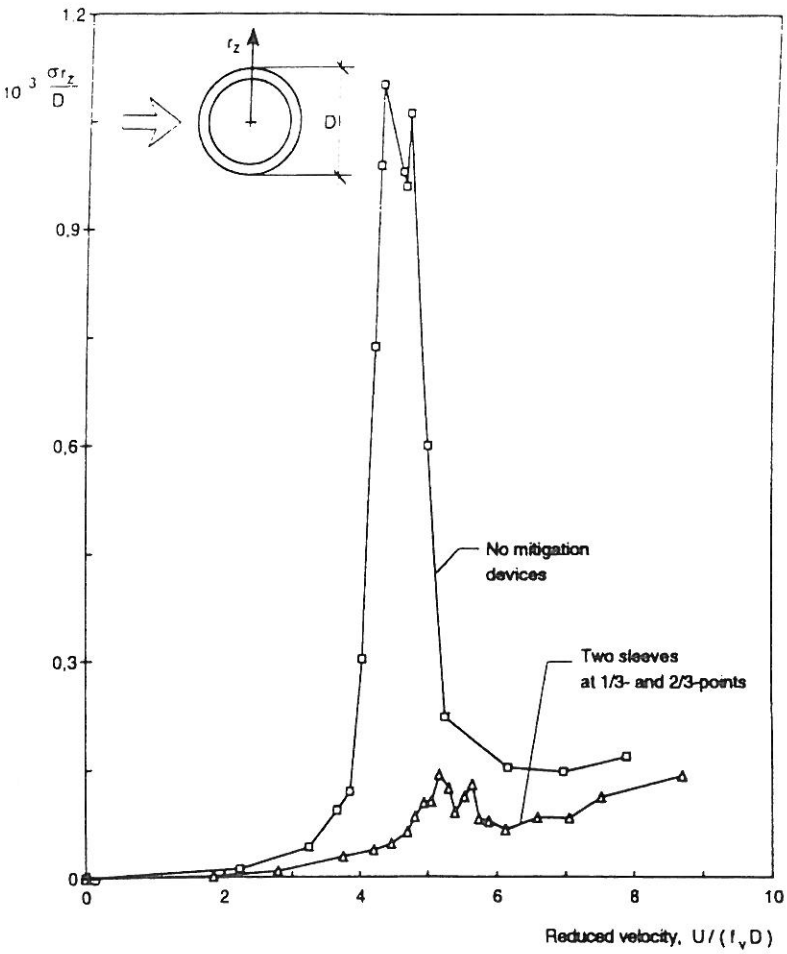

Figure 10. Model Testing / Gullfaks B. RMS Crosswind Displacement Response

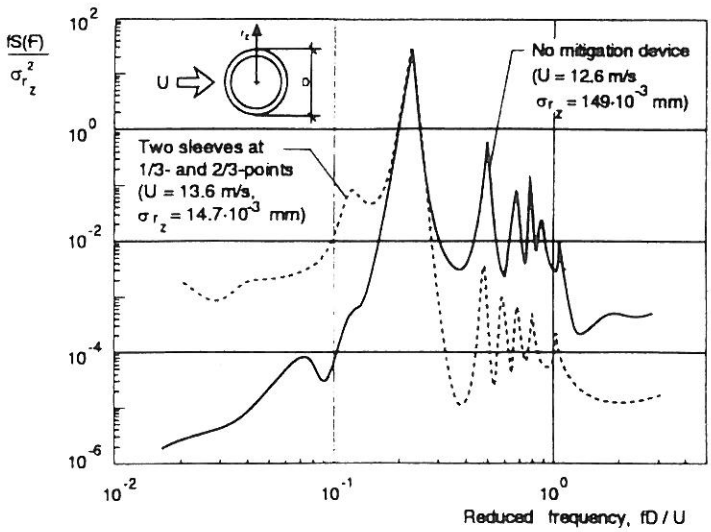

Figure 11. Model Testing / Gullfaks B. Spectral Density of Displacement Response.

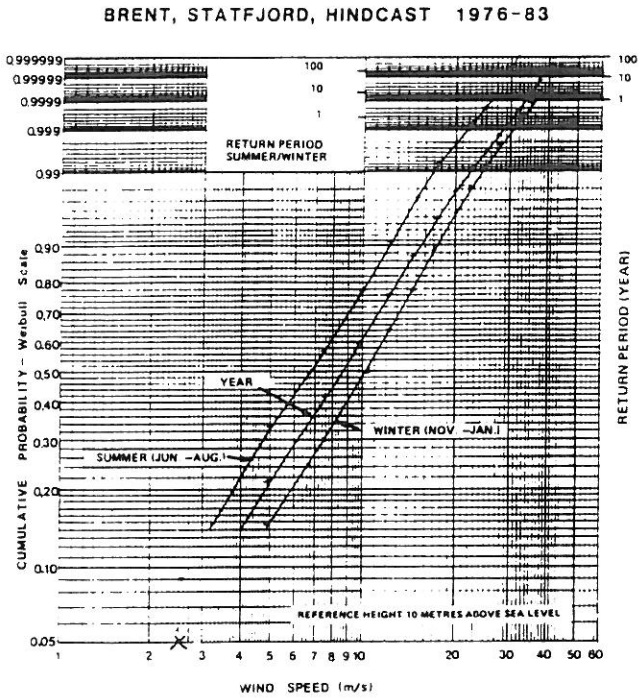

Figure 12. Cumulative Distributions for the 1 h wind speed.

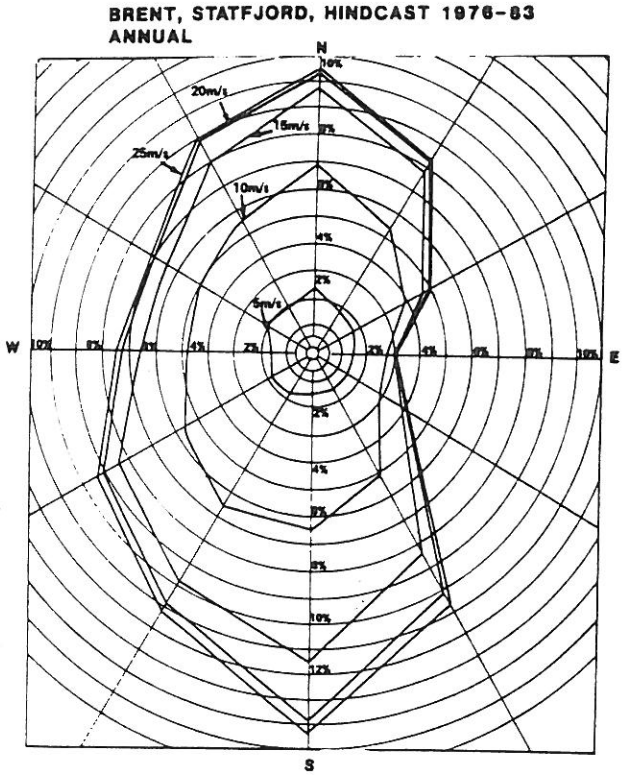

Figure 13. Annual Wind Frequency Distribution.

Table 1. Geometry Parameters and Fatigue Response.

	L [m]	D [m]	T [m]	N [hz]	Sc	Vc [m/s] 1)	Re 2)	Fatigue 3)	Observations
STATFJORD A									
1. Main tie chord	5,8	0,2191	0,0159	19	26	21	307	OK	Vibrations
2. Main tie brace-hor.	3,312	0,1143	0,0063	52	20	30	226	<1	Vibrations, possibly
STATFJORD B/C									
1. Wind strut chord	5,3	0,2191	0,0159	23	26	25	368	OK	None
2. Wind strut brace-hor.	2,588	0,0889	0,0055	66	22	29	173	<1	Cracks
3. Wind strut brace-vert.	3,29	0,168	0,01	77	21	65	725	OK	Crack, indication
GULLFAKS B									
1. Wind strut chord	4,5	0,1524	0,01	22	23	17	173	OK	Crack, indication
2. Wind strut brace-hor.	5,2	0,1016	0,01	18	34	9	62	10	Cracks
3. Wind strut brace-vert.	3,5	0,1016	0,01	40	34	20	136	<1	Cracks
HEIMDAL									
1. (290-314)	15,4	0,273	0,01333	6	18	8	143	7	Vibrations, singel
2. (313-314)	12,9	0,273	0,01333	8	18	11	204	<1	Vibrations, frame
3. (313-294)	10,9	0,219	0,01333	9	22	10	146	2	Vibrations, frame
4. (294-292)	17	0,324	0,01333	6	15	10	198	<1	Vibrations, frame
ODIN									
1. (43-52)	7,25	0,273	0,0127	26	17	36	648	<1	Cracks
2. (43-42)	5,5	0,273	0,0127	45	17	62	1128	OK	Cracks
3. (23-20)	9,73	0,324	0,0127	17	14	28	607	<1	Cracks
4. (33-30)	8,55	0,323	0,0127	22	14	36	778	<1	Cracks
5. (33-40)	9,45	0,273	0,019	15	25	21	373	<1	Cracks
VALHALL PCP									
1	13,32	0,406	0,013	12	12	24	642	<1	Cracks
2	9,39	0,324	0,013	19	15	30	651	<1	Cracks
3	3,07	0,406	0,016	218	14	442	11988	OK 4)	Cracks

Notes:
1. Critical velocity, Vc=1/St*N*D, St=0.2
2. Reynold's number at critical velocity in thousands
3. Fatigue life of single element joints in years; indicative
4. DIN 4133 not applicable; k-brace.