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BETREFFENDE:

Design against fatigue and fracture for marine structures.

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DESIGN AGAINST FATIGUE AND FRACTURE FOR MARINE STRUCTURES

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ABSTRACT

Design in connection to cracking and fracture aims at structural integrity at low costs.
Integrity and safety are to a large extent determined by material quality and welding effects (defects, residual stresses, notch toughness of weld and H.A.Z., deformations) and the way these effects are measured and controlled. The estimation of the consequences of these effects and design geometry for fatigue and fracture in marine environment is an essential step in the design procedure.
The reliability of the answer depends strongly on the amount of sophistication put into quality control tests and fatigue calculations. The paper will discuss a number of weak parts and inconsistencies inherent in current design procedures and why these yet seldom have given rise to great trouble.

1. Introduction

The word design has several meanings. It may be design procedure or design calculations or the actual structure.
In the present paper both procedure and actual structure will be discussed.
In the first part of the paper the relative importance of shape and material properties of structures are considered from the viewpoint of cracking. It will be seen that material properties greatly determine safety with respect to brittle fracture while from the viewpoint of fatigue shape is most important. This is not unknown to many people, but it is not generally realised whether fatigue contributes little or much to the danger of brittle fracture.
Apart from that with nowadays' steels and welding methods brittle fractures in ships can be avoided with extremely high probability.

The second part of the paper discusses current design procedures in connection to fatigue. There is a need for more sophistication in calculations for crack growhth, but this need may disappear when more attention will be paid to shaping of details and welds.

2. Influence of shape and material on the fracture-strength of structures

Our knowledge about the real brittle-fracture strength of ships is not large. This is mainly a consequence of the fact that the brittle-fracture-strength of ships is large. For, when this statement would be false, ship-fractures would occur more often and our knowledge would improve.
Another reason for the first statement is that realistic experiments with ship structural details in laboratories have become practically impossible in the course of years due to the increase in size of the ships. For instance testing a hatch corner of a large bulk-carrier would necessitate testing machines of capacities of some 10.000 tons.
From the second statement of this paragraph it follows that there is also little impetus from practice to carry out such costly experiments. Another argument is that the knowledge and insight concerning the problem of brittle fracture (initiation) is probably good enough to make large scale experiments superfluous. Small scale testing can be relied upon with confidence.

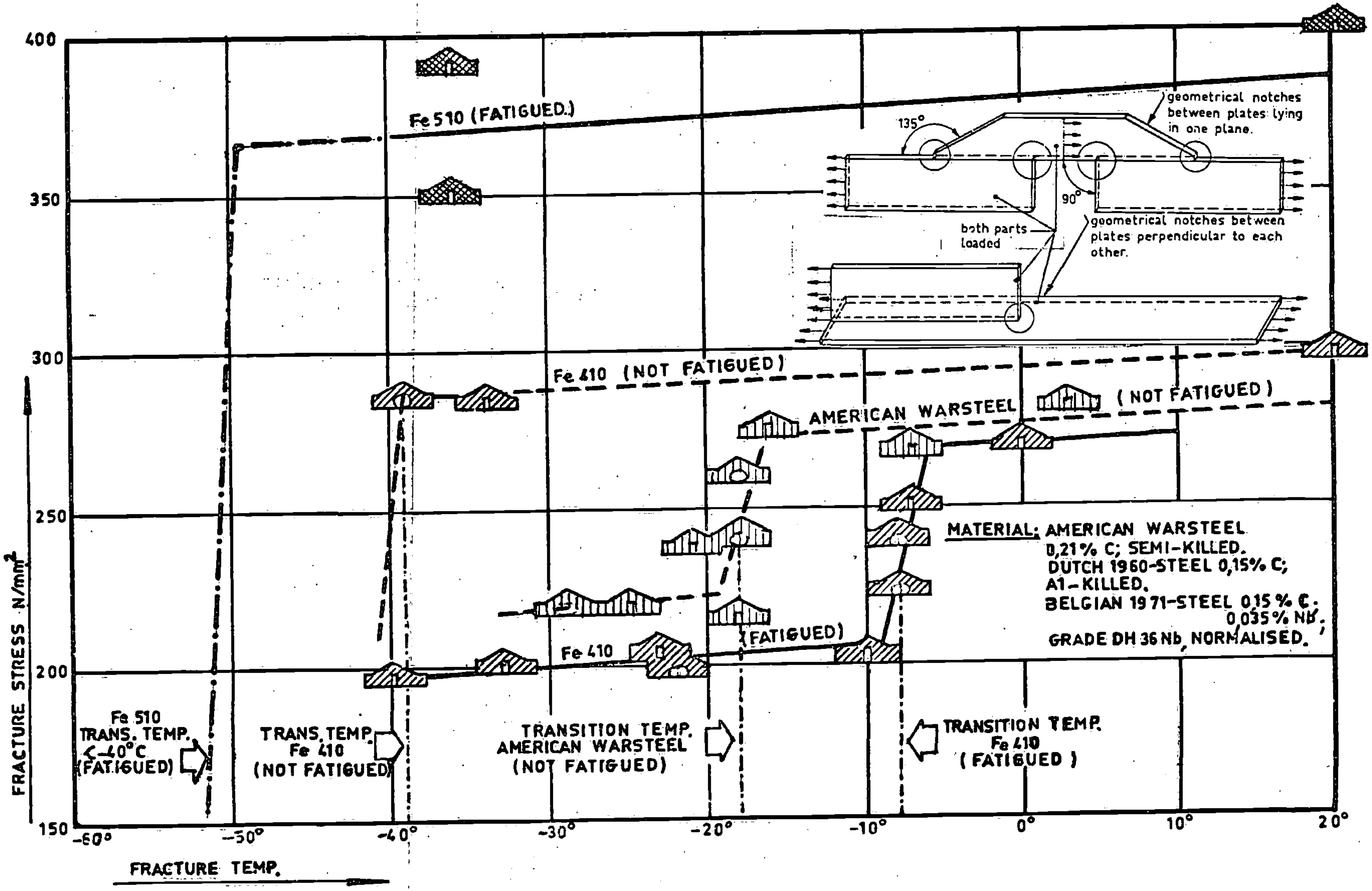
It will be shown that in a general sense this picture is true. But caution is necessary, in case of thick plates which points to offshore structures.

As said before, results of full-scale brittle fracture experiments with ship structures are scarce, but they can nevertheless be found. Most of these have been reported in the proceedings of the committees on brittle fracture and fatigue of the International Ship Structures Congress /1/. In the present paper only one series of experiments will be mentioned. Apart from some subjectivism in the choice, the reasons are the following:

a. The experiments embrace:
   1e. Steels of World War II used in Liberties and T2-tankers /2/.
   2e. Common C-Mn-steels in the usual Al-killed condition of the years 1960 (mild steel Fe 410), /3/.
   3e. Nb-containing fine grain steels (Fe 510) in use nowadays when grades D and E are required in large ships /4/.
b. All specimens were of equal design and dimensions.
c. The post-war specimens had been subjected to fatigue-loading prior to fracturing at low temperature.

Figure 1 shows the results.

Figure 1. **INFLUENCE OF MATERIAL QUALITY ON THE LOW-STRESS/HIGH-STRESS TRANSITION TEMPERATURE OF BOTTOM LONGITUDINALS**



Looking first to the American results with war-steel it is remarkable that the transition temperature of the specimens is as low as $-20^{o}C$. This is some $20^{o}C$ lower than the temperature at which brittle fractures have started in T2-tankers during and after the war.

The use of Dutch 1960 C-Mn, Al-killed steel resulted in a substantial improvement of some $20^{o}C$ in transition temperature. From this it might be concluded that in that time ships had become absolutely safe from brittle fracture. But this statement was not confirmed by the experience from practice /5/. Apparently the (full-scale) experiments were still not sufficiently realistic. Indeed, static loading of a virgin specimen is more favourable than the conditions which ship structures meet during their life. The cyclic loading due to wave-bending will cause fatigue-damage (cracking and deterioration of the material). It was thought that especially small cracks might impair the fracture strength, because the tips of these cracks will be situated in the weld zone. The results were really alarming: the transition temperature rose from $-40^{o}C$ to $-8^{o}C$. This approaches the temperature region of interest to ships.

The investigation provided the proper explanation for the discrepancy between the American results and practical experience. They had not been cyclically loaded prior to fracture, for otherwise the transition temperature would have been about $+10^{o}C$ ($-20^{o}C$ (static) + $30^{o}C$ (fatigue)) instead of $-20^{o}C$.

We now come to the situation nowadays. Figure 1 shows that for Nb-normalised fine grain steels the transition temperature of specimens with fatigue cracks was at least $30^{o}C$ lower than the one for the 1960 C-Mn-steels. In other words the results for the modern steels with fatigue cracks are as good as those for the 1960-steels without fatigue cracks.

A number of important observations can be made:

1e. Safety with respect to brittle fracture is directly and mainly dependent on material (inclusive weld!) quality.

2e. Design has an important indirect influence due to its effect on the development of fatigue cracks.

3e. Brittle fractures in ships can only occur after extensive yielding. Due to that residual stresses cannot exert a direct influence. Some indirect effect is present in connection to fatigue.

4e. Another consequence of 3e is that the yield point governs the brittle fracture strength of ship structures. In figure 1 the fracture strength is equal to yield strength for all experiments above the respective transition temperatures. Below these temperatures all fractures started after 1% yielding of the bottomplates in the fracture section over the full width (see /3/). (This yielding was a consequence of axial loading plus overall bending). In fact the transitions in figure 1 are no real "high stress - low stress" ones but "general yield - extensive local yield" ones.

5e. There was satisfactory correlation between the 21 Nm Charpy-transition, and the indicated transition temperature of the fatigued specimens. The Nil-ductility temperature was slightly too optimistic.

It is not suggested that the foregoing covers the whole brittle fracture problem for maritime structures!
For instance at crossing welds hot-straining embrittlement may occur (Greene-Wells effect /6/), which may trigger a brittle fracture. Nowadays the probability of occurrence will be very low, but still within practical possibilities. It is fortunate that with actual steel qualities there is a large chance that such a fracture will be arrested immediately after initiation.
A final problem is the welding of thick plates (> 30 mm) with high heat-input (electroslag or electrogas). Shifts in transition temperature of some 100°C are possible in the heat-affected zone. Wide-plate testing of fatigued specimens with transverse welds has proved that brittle cracks may keep running within heat-affected zones of only 2 mm wide. The residual stresses cannot exert any influence on the fracture path due to their low gradient /7/. For offshore structures the fear of too high heat input has become so large that people have recoursed to extremely high numbers of weld passes. Even then satisfactory notch toughness could only be obtained by post-weld heating at 600°C, (stress-relieving).
The low qualities were probably caused by mutual hot-straining embrittlement of different layers. Often better results will be possible by a limited number of passes, say 12 to 16 in 50 mm plates.
By the way, C.O.D. (Crack Opening Displacement) testing is indispensible for thick joints, despite some opposition from people who do not succeed in meeting the requirements involved (see also 4).

3. Design in connection to fatigue

In 2 the importance of geometry in connection to fatigue, and of the latter in connection to brittle fracture, has been discussed. The present paragraph will deal with design procedure in connection to fatigue. That promises a lot more than will be treated actually. The reasons are that:

a. space is lacking for a thorough discussion;
b. in the literature a number of excellent relevant papers has appeared in recent years; they can certainly not be improved by the author;
c. there are still white and black spots in proposed procedures and philosophies. It is on these that the author likes to focus attention.

Fatigue is a fast developing science. There will be not so many fields in technics in which as much money is spent, especially in experimental research. This is partly due to the fact that experiments take a lot of time. In this respect the situation has become even worse since corrosion fatigue necessitates long lasting low-frequency testing.
On the other hand crack-propagation studies combined with fracture mechanics can lead to an important reduction in number of specimens and testing time as compared to Wöhler-testing (figure 2).

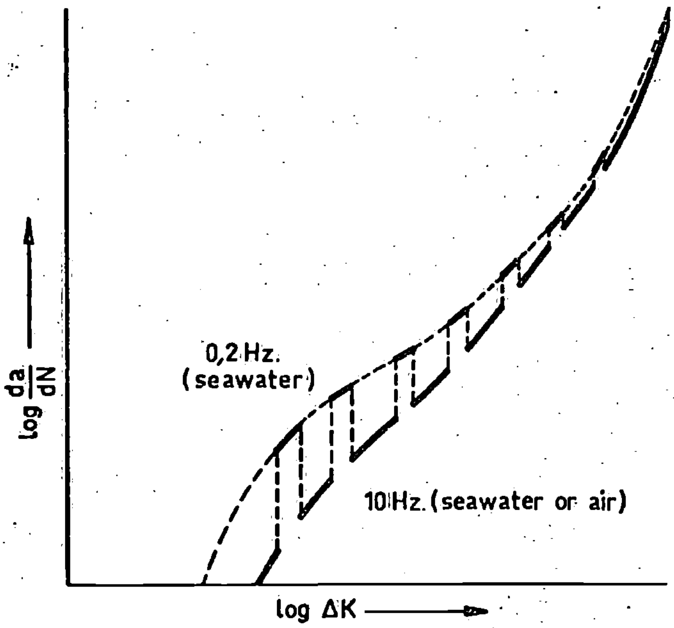


Fig. 2 Accelerated corrosion fatigue testing.

It is only a pity that in structural specimens measurements of crack lengths are very difficult, especially when the specimens are tested in seawater.
This might be one of the reasons that existing design procedures are mostly using Palmgren-Miner's rule and Wöhler curves for fatigue calculations instead of crack-propagation calculations.
Another reason is that the rule $\sum \frac{ni}{Ni} = 1$ is certainly not illogical or unrealistic. It is easily understandable and simple to work with.
Very important is, that it forms a basis of reference for programmed and random loading: results can be expressed in terms of deviations of Miner's rule. It is often thought that the rule is only applicable for the phase of life which is spent for crack-initiation. The argument is that during the crack propagation stage ΔK does not remain constant, so that later load packets would give more crack growth than former ones (sequence effect). But already in 1974 Frost, Marsh and Pook /8/ showed that for constant m crack growth is independent of sequence of cycles. Schütz /9/ has discussed extensively Miner's rule and some improvements against the background of test results (mainly for aircraft materials and structures). The rule did not come out unfavourably. Yet it is the author's firm belief that with the aid of fracture mechanics for crack propagation a better balance may be obtained between the efforts spent for obtaining information about wave-induced loads and for calculating hot-spot stresses on one hand, and the capability of a structure from the viewpoints of fatigue and permissible crack-length on the other hand. A look into the proceedings of conferences dealing with fatigue, into the publications in journals devoted to fracture, fracture mechanics and fatigue

will lead to the conclusion that both in theory and experiments much is going on which is of use for arriving at reliable crack propagation calculations for structures. The problem is that we need some standard procedure(s) acceptable to classification societies incorporating those items of crack propagation calculations which have met general or wide agreement. Such a procedure might be valid for 3 or 5 years, after which adjustments can be made.

As long as this is not obtained, the use of Miner's rule has to be preferred if only for reasons of safety. For, without standard procedures it cannot be avoided that self-made methods for crack propagation calculations involving corrections for crack closure,Elber effect, plastic zones, relief of welding stresses, residual stresses after overloads, strain hardening and softening in plastic zones etc., will lead to widely differing results. This will also be caused by the fact that the input of the loads into the calculations can be done in different ways, see /9/. For instance there are the cycle-to-cycle method and the equivalent constant RMS-stress method. The latter can be based on short or long periods related to changes of weather, loading conditions, routes or seasons. Each method has its specific problems. For instance in the RMS-approach the main problem is which factor times RMS gives the proper equivalent stress for constant loading. This factor must be dependent of N because RMS is the same for short and long periods within stationary conditions. The point is illustrated in figure 3.

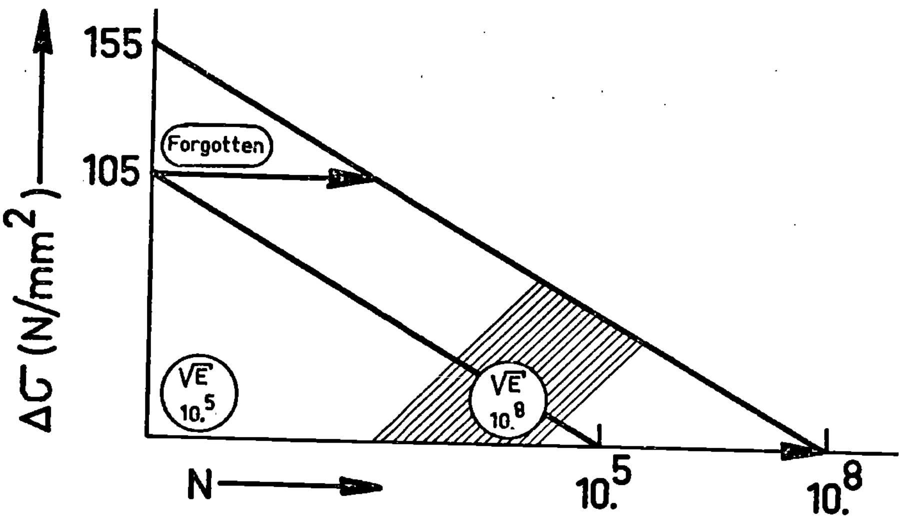


FIG.3 UPPER PART OF FIGURE IS NOT INCLUDED IN $\sqrt{E}$-TREATMENT.

It means that the factor $\sigma_{eq.}/\sigma_{RMS}$ has to be a function of the number of cycles in each block of the whole load history. For Rayleigh distributions the equivalent stress should take the form of $\sigma_{eq.} = c\sqrt{E.\ell nN}$. c Will be in the order of magnitude of 0.3. (In /13/ the stress equivalence factor is given in terms of slope b of Wöhler curves for narrow band random loading. Applying Miner's rule they found $\sigma_{eq.}/\sigma_{RMS} = \sqrt{2}|\Gamma(1 + b/2)|^{1/b}$. It apparently applies to large N. Then the $\sigma_{eq.}$ is severely underestimated in the author's opinion).

Both methods mentioned allow to take into account certain sequence effects.
In the RMS-method this applies only to the (important) bad-good weather variations and changes of mean stress (loading conditions, direction of wind and tide streams for offshore structures). In the cycle-to-cycle method the calculations can be made as realistic as the input information (load data) allows.
But it should be realised that in both methods crack growth data, obtained from constant load tests are used.
Other methods exist of which the quasi-stationary random method is the best for marine structures, but also an expensive one. They will not be discussed here.
In /9/ and /12/ relevant information can be found. The purpose of the present paper is to show that because of a lot of parameters involved in crack-propagation calculations for maritime structures, and the existence of an overwhelming amount of specialised papers on the subject, a confused situation has emerged.
In it it is very difficult to get a proper idea about whether or not certain calculation procedures lead to reliable and accurate answers (and their confidence limits!).
One up-to-date standard method could be used as a reference for judging other methods, improvements and deviations in case of special structures or conditions.

There have been made already important steps in the right direction. Standardisation of wave spectra started even tens of years ago. But the aim was not (so much) fatigue calculations. Haibach et al. /11/ proposed a standard random load sequence for fatigue in 1976. The author knows about an, as yet unpublished, paper by L.P. Pook on standard load histories for offshore structures. A very extensive discussion in the direction of procedure standardising from the fatigue (capability) point of view has been given by Francis, Lankford and Lyle in /12/. Yet it does not go so far as the author advocates, as is evident from page 16 where theories are excluded which "require impractical (!) data input, such as knowledge of the plastic zone size at a crack tip". Nevertheless the paper presents a wealth of data as well as methods in a form that allows the reader to put in his own ideas and judgments. Other interesting and/or useful papers have been published in proceedings of the BOSS /14/ and the Offshore Technology Conferences (O.T.C.).

In the abstract of the present paper it has been promised to show why despite rather poor fatigue calculation methods, practical experience with offshore structures is not alarming.
This will be discussed in the next section.

4. "On the safe side" design procedures in practice

Fatigue and fracture analysises for offshore structures largely tend to be on the

safe side. For instance for S-N-curves (Wöhler) for welded connections, lower regions of scatterbands are used. Welding stresses are always taken tensile and equal to yield point. Crack closure is neglected. The beneficial influence of tensile overloads, both in connection to welding residual stresses as from the pure fatigue point of view, is not taken into account. Also it is seldom realised that in brittle fracture control the existing (Charpy) specifications have emerged from practical experience and consequently are not "averages" but "safe" values. On the other hand there are also approaches which are too optimistic. Post-weld heat treatments are not always as beneficial as is hoped. It may give rise to cracking, destroy compressive residual stresses at critical points or - in case of heating parts of existing structures - bring forward new stresses and deformations. Furthermore it can (and will) be shown that the generally held idea that high stress fatigue strength is not impaired by corrosive environment, is not justified. The influence of neglection of changes of mean stress has been discussed earlier /10/ and has also been found for aircraft-materials /9/.
In the following a case will be discussed, in which every possible aspect of fracture analysis was on the safe side. The whole story is no fantasy, but reflects an actual stage in the design of an existing offshore structure!

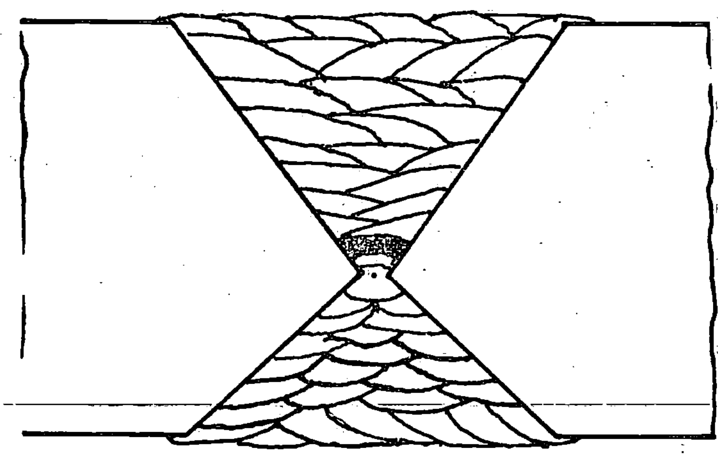


Figure 4.

The problem started when it was observed that in a multi-run X-weld in a thick plate (figure 4) the specified C.O.D.-values could not be met in the as-welded condition.
The critical crack lengths calculated from the measured C.O.D.-values were in the order of magnitude of only a few mm's. The crack lengths calculated on the basis of expected loads in 20 years, hot-spot stresses, N.D.T.-defect lengths, Miner's rule and B.S.153 S-N-curves, were about ten times as large as the critical ones. The situation seemed to be hopeless. The decision was taken to replace several meters of welds, and heat-treat others on the spot.
In the author's opinion, the outcome would have been different, when not every part of the analysis had been unduly conservative. The main point was a complete neglection (or misunderstanding) of the role of the residual welding stresses. When a multi-run X-weld is made by alternatively laying beads on both sides of the plate, the residual stresses are tensile at the surfaces and compressive at the root of the X. Important defects are mostly only present in the root (slag inclusions, lack of penetration, root cracks). Consequently crack growth, if any,

will start at the root.
Now, the fatigue calculations were made according to a standard procedure. In it it was stated (as usual!) that tensile residual welding stresses are present around defects and should be taken into account. Yet in the case considered the welding stresses were compressive! (Most unrealistic was that even for structural parts which were loaded in compression, fatigue calculations had to be made, because of the presumed presence of tensile welding stresses in the X-roots!).
A calculation procedure in which the compressive residual stresses were simply excluded resulted in zero crack growth!
But this is not yet the whole story. The C.O.D.-testing for estimating critical crack lengths had been carried out in a way which also suppresses the beneficial effect of compressive welding stresses in the centre where toughness is worst.
It is well-known that in order to be able to supply a C.O.D.-specimen with a straight fatigue-crack, precompression in the thickness direction of the notched zone is applied. This has two effects: elimination of the welding stresses, and strain hardening of the material /15/. The latter will be aggravated by the cyclic loading of the specimen (but that corresponds rather with what may happen in a structure).
But the first two factors may reduce substantially the C.O.D. of the weld metal.
Consequently the calculated critical crack lengths will certainly be smaller than what is justified.
The reader will observe that both from the demand- as from the capability point of view the approaches were (very) pessimistic. This often happens, although not always as drastic. But it will be the cause that despite many mistakes in the design procedures practical experience with offshore structures is not too bad.
In the next section some of the arguments given here will be explained further.

5. Optimistic and pessimistic arguments with respect to design procedures

a. Residual welding stresses

Welding stresses have a clear influence on the fatigue-strength for constant amplitude/constant mean stress loading.
The smaller the cyclic stresses the larger the influence. Therefore Wöhler-curves obtained from small, or unwelded specimens are corrected as in figure 5.
There is nothing wrong in this as long as these curves are not used for variable amplitude loading. For, in actual structures welding stresses disappear quickly when incidental high loads occur. The first storm will do the job. It is often thought that this is only true in case the nominal stresses approach yield point.
But figure 6 demonstrates that for a mild discontinuity being a circular hole, a nominal stress of only one third of yield point eliminates the welding stresses

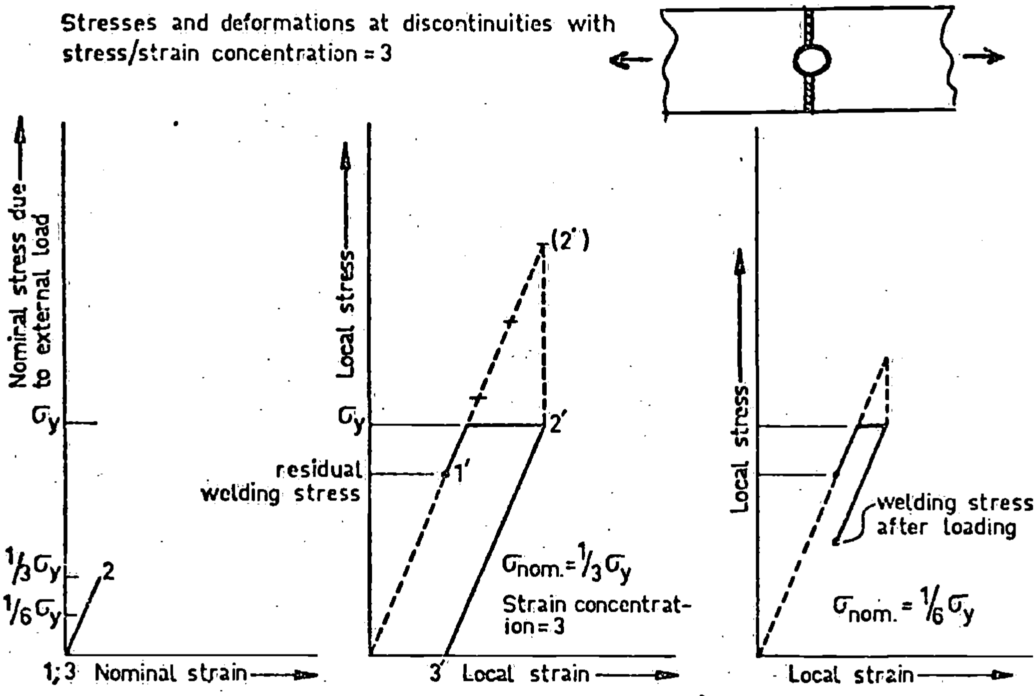


FIG 6 THE DISAPPEARANCE OF WELDING STRESSES BY HIGH LOADS.

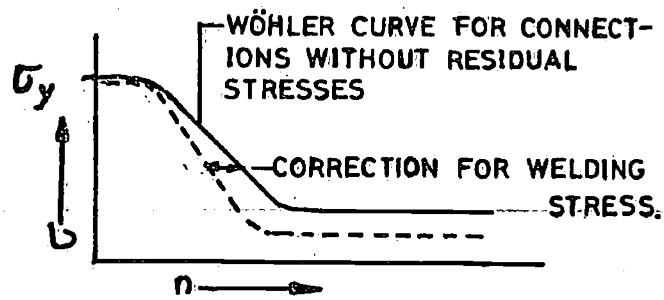


FIG. 5

completely. In ships and offshore structures much higher stress (strain) concentrations are present. Precisely at these points where the danger of cracking is greatest, the residual stresses are soon relieved.
Another point is that in case cracks nevertheless develop, the cracking itself will also relieve welding stresses. It may be concluded that for the greater part of the life of a structure, residual stresses cannot exert a bad influence. This means that Wöhler-curves may be used without correction for the presence of welding stresses. Even curves for stress-relieved specimens might be used, provided the stress-relieving has not an effect on the material properties. (Such an effect - if favourable - would be the only justification for post-weld heat-treatments).
But whether or not the Wöhler-curves are corrected is far less important than the fact that in the absence of residual stresses the phenomena of crack closure and the Elber-effect can occur. This may cause increases in fatigue-life in the order of magnitude of a factor 5 /15/. For, when a crack of a few mm's has formed, the

compressive part of a load cycle has become insignificant (see figure 7 from /16/).

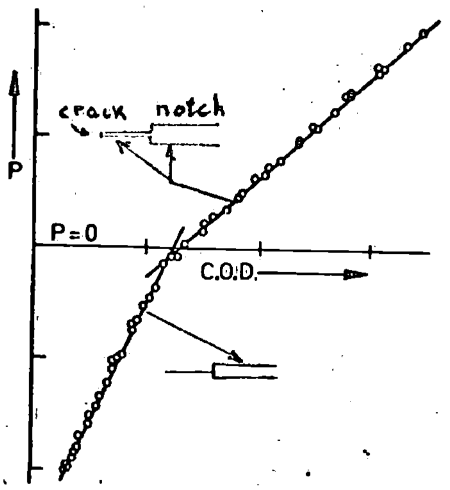


FIG.7a P-C.O.D. DIAGRAM AFTER 37100 CYCLES FOR CRACKLENGTH 10 m

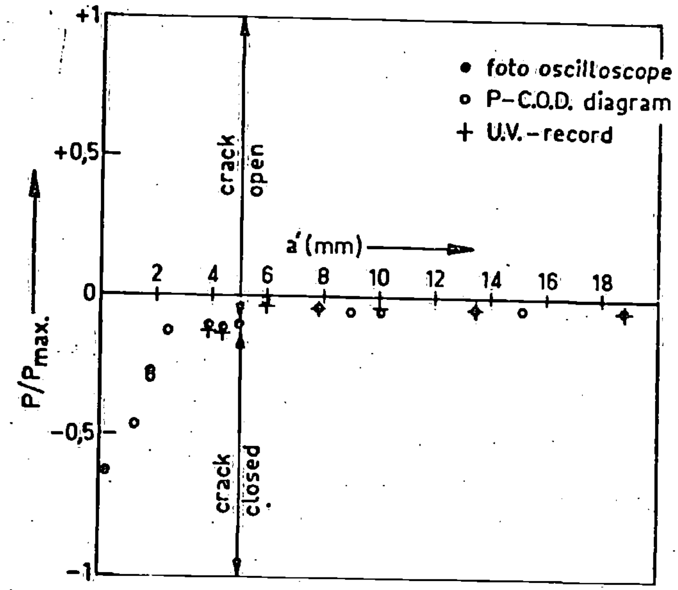
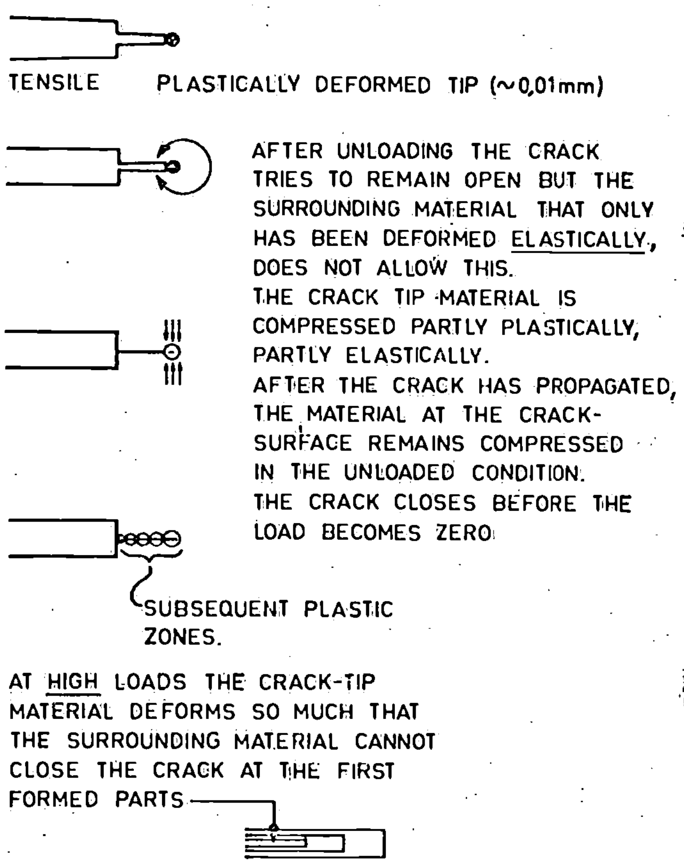


FIG7b ALTERNATING BENDING PART OF COMPRESSIVE LOAD DURING WHICH CRACK REMAINS OPEN.

In figure 8 the Elber-effect is explained /17/. In the Delft Ship Structures Laboratory it was confirmed that the effect was also very prominent for high-cycle repeated bending loading in air and seawater (thickness 28 mm, see figure 9a). It constituted a reason for studying the effect also in repeated axial loading on a centrally notched 500 mm wide plate of 19 mm thickness.
The plate was instrumented with strain gauges and C.O.D.-meters as indicated in figure 9b. It can be seen that for some 90% of the time tested the effective load was only about 75% of the real load. From both figures 9, it follows that this reduction in fatigue load occurs as well in high-stress as in low-stress fatigue.

b. Ultra low cycle - corrosion fatigue

This section will start with a quotation from a paper of Det norske Veritas /18/ on corrosion fatigue: "In the low cycle fatigue range, normally defined to be less than 10^5 cycles, the deterioration promoted by seawater is less".
This is a generally held opinion. The arguments are in the sense that the crack growth is faster than the penetration rate of the corrosive environment. The cyclic frequency of the high loads is apparently taken equal to that of the lower loads ($\sim$ 0.1 Hz for ships). Figure 10 taken from /19/ and figure 11 from /20/ allow another look into the situation. Figure 10 shows that one "built up" stress-change of 270 N/mm^2 has occurred in a containership during a severe storm. The average level of wave-induced bending stresses was much lower. In Aertssen's paper it can be found that severe slamming occurred two to three times per hour.
So the frequency of these was not in the order of magnitude of 0.1 Hz but 0.001 Hz.



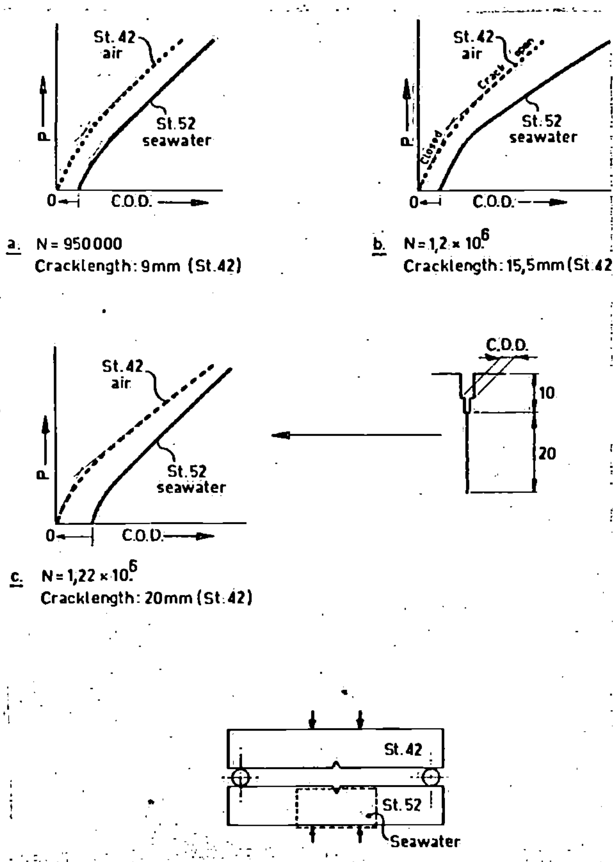


FIG. 9a CRACK CLOSURE DURING REPEATED LOADING.

FIG. 8 CRACK CLOSURE AND THE ELBER-EFFECT.

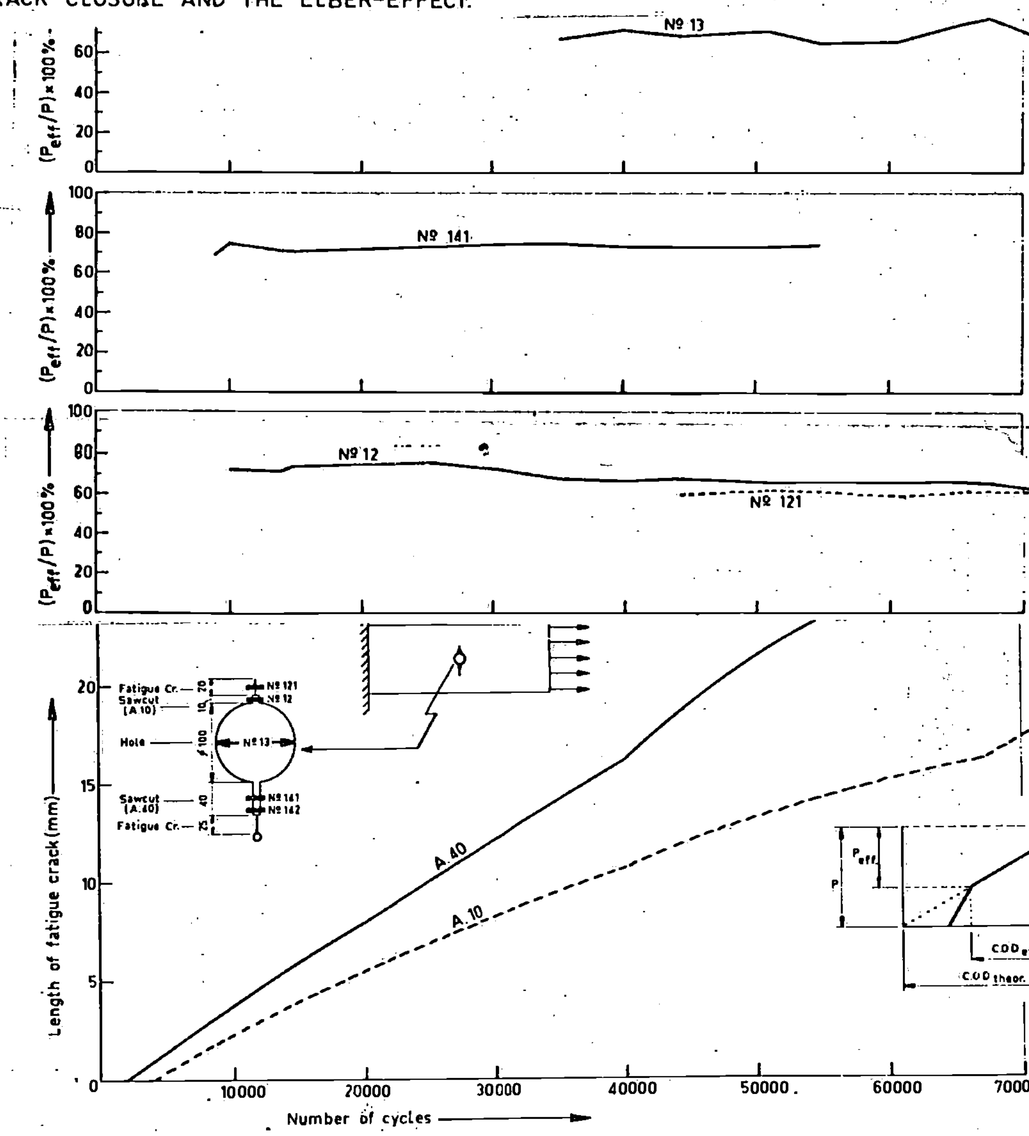


FIG. 9b CRACK CLOSURE IN REPEATED AXIAL LOADING.

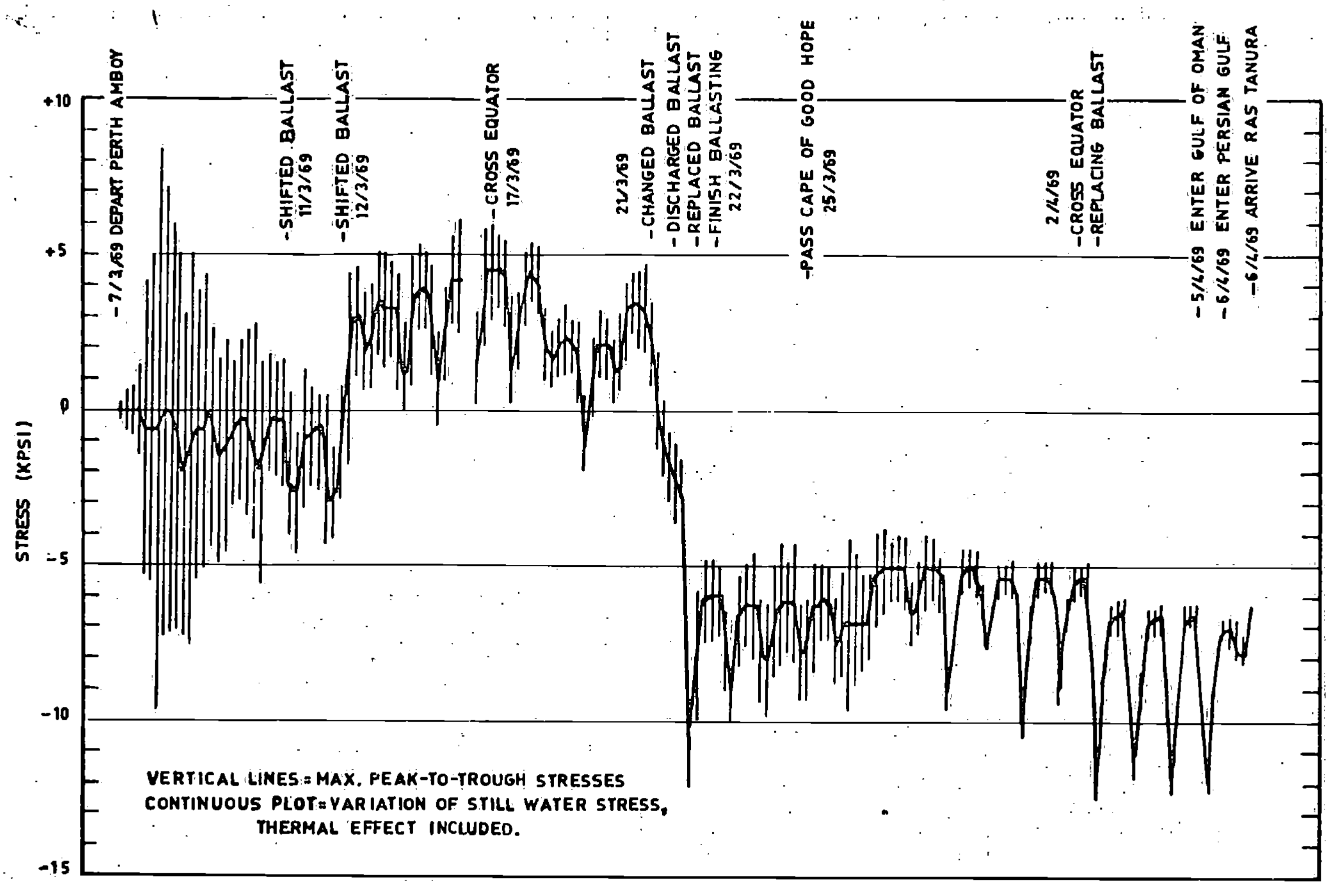


Fig. 11. Typical Voyage Variation of Midship Vertical Bending Stress, ss R. G. FOLLIS

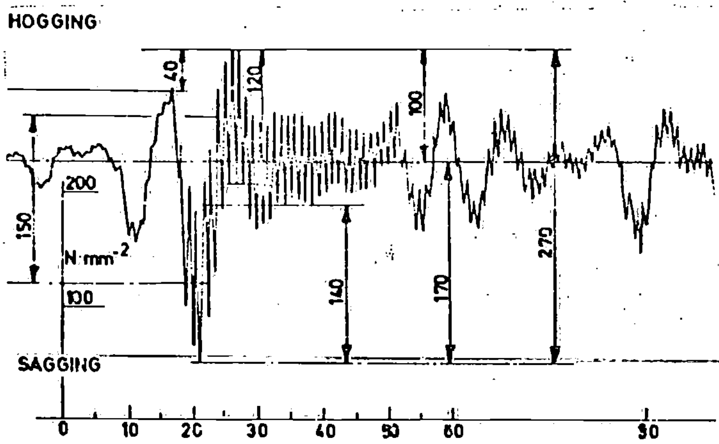


Figure10. Whipping stresses in upperdeck of containership IV, Beaufort 10.

Figure 11 shows other very low frequent variations of stress. In order to get an idea about the corrosion fatigue damage caused by ultra-low frequent extreme stress cycles, the author carried out the experiments shown in figure 12. Two specimens were tested simultaneously, one in air, one in seawater. The loading program was as indicated below right in the figure. For the first two specimens the experiment started from a sawcut. For the other specimens the sawcut was firstly extended 1 mm by fatigue loading at 4 Hz before the low-frequent loading started. The first experiment started with 0.0003 Hz. After about 1500 cycles the crack in the seawater specimen was nearly 10 mm in length. In the air-specimen it was only 0.5 mm. After that stage the frequency was increased to 0.00084 Hz. At first some retardation occurred but soon the crack-growth in seawater continued at high rate, although not so high as before.

This result applies to mild steel (Fe 410). Three more tests have been carried out with Nb-containing, normalised Fe 510 at two stress-values and two frequencies. There was a distinct difference between the behaviour at 0.0017 Hz and 0.01 Hz.

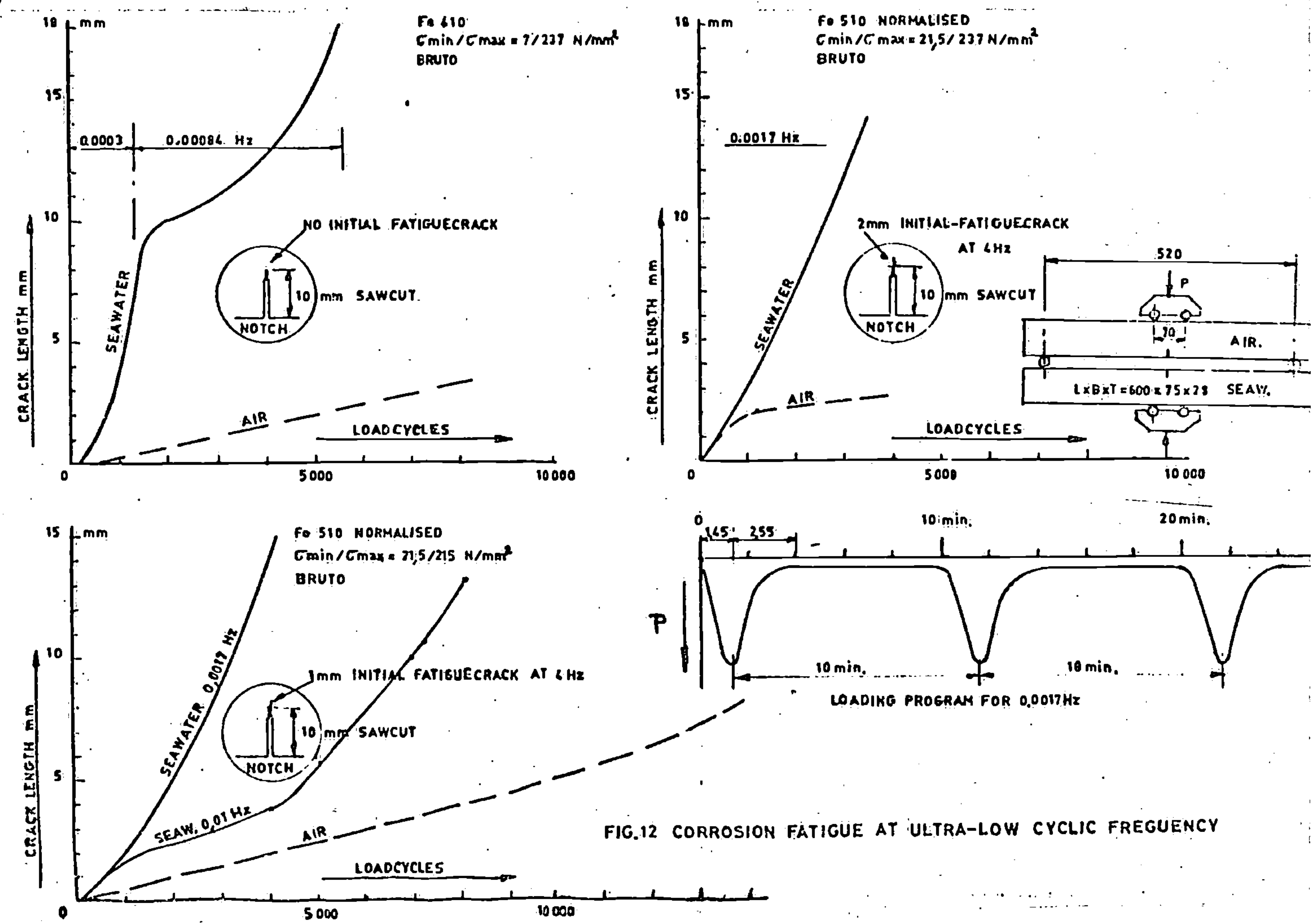


FIG.12 CORROSION FATIGUE AT ULTRA-LOW CYCLIC FREGUENCY

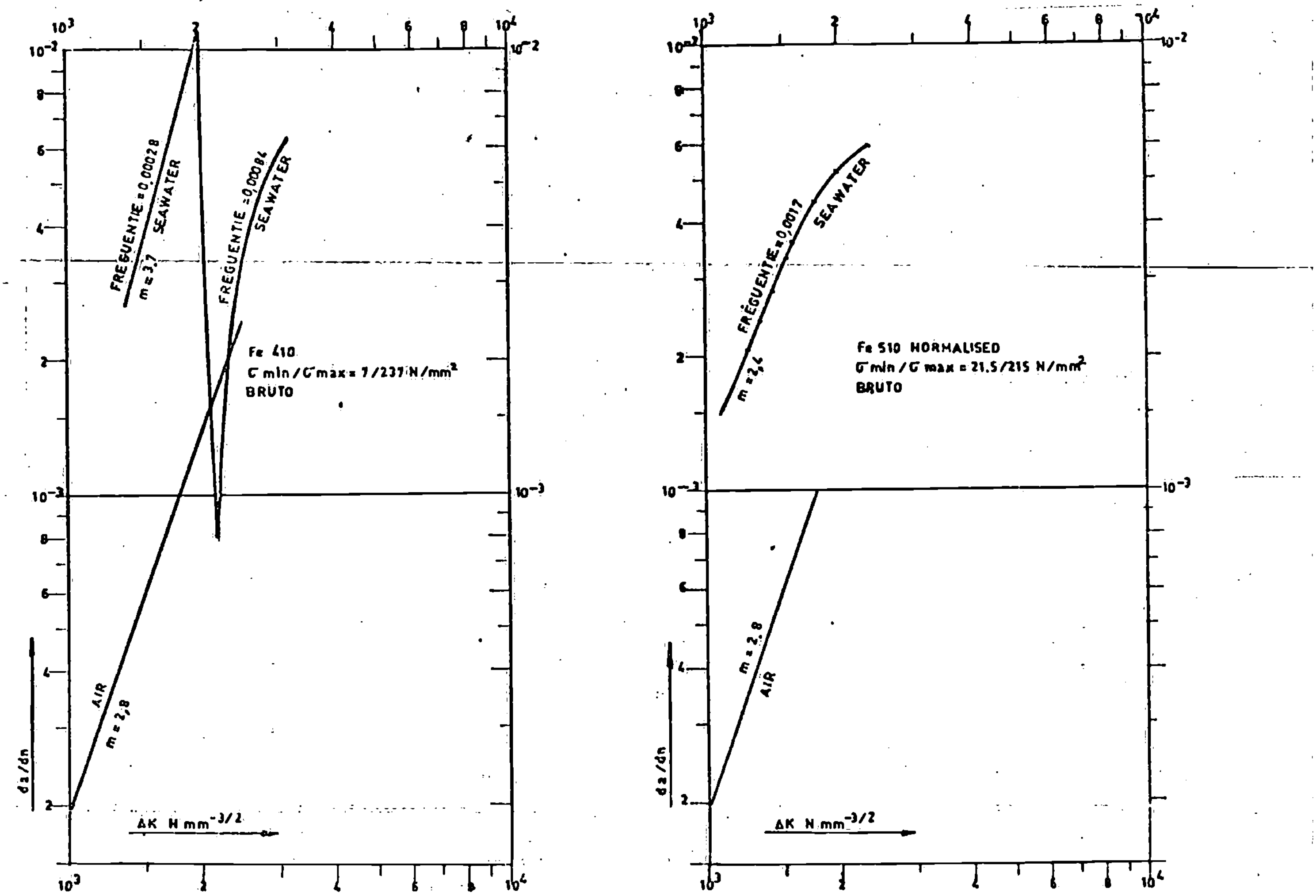


FIG. 13 da/dn-ΔK CURVES (AIR AND SEAWATER) FOR ULTRA-LOW CYCLIC FREGUENCY

In the first case the crack growth was about 5 to 10 times faster in seawater as compared to air; at 0.01 Hz it was only 2 to 3 times.
In figure 13 a da/dn-ΔK plot is shown. It is remarkable that the difference between seawater and air becomes manifest in the vertical position of the curves (c-value) and not in the inclination (m-value).

In conclusion it may be said that extremes occurring at large intervals contribute effectively to crack growth in seawater. A few thousand changes of hot-spot stress between 0 and σ_y at places where weld defects are present may lead to some 10 mm crack extension.

CONCLUSIONS

1. Classification societies and (other) fatigue-experts should develop a standard method for calculating crack growth. It should take into account actual knowledge and theories about plastic zone sizes, strain hardening, crack closure etc.
2. The standard procedure should (also) act as a reference for checking new theories and should be corrected every 3 or 5 years.
3. Residual stresses in maritime structures are hardly harmful from the point of view of fatigue.
4. In corrosion fatigue low frequent changes of high stress are more dangerous than generally thought.

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