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FRACTURE-SAFE DESIGN OF MARITIME STRUCTURES

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1 *Introduction*

Design in connection to cracking and fracture of structures should:

a - be a *fully integrated process*. For instance the loading parameters and the permissible values of stress, strain and crack length should not be treated separately as they are interdependent. This will be made more clear in sections 2 and 3.

b - be a process in which every step must be defined in statistical terms (probabilities, confidence limits). This is not purely a consequence of the unavoidable statistical description of sea-induced loads, but also of the - not exactly to define - "capability" of structures and of weaknesses in theories and suppositions.

c - be 100% realistic. This means that it should not deal with models made of homogeneous, isotropic, ideally elastic materials, but with *man-made, welded* constructions, containing misalignments, defects, residual stresses and locally damaged (embrittled) materials. In this connection it is emphasized that defects are always present in welded structures and that these defects have to be looked upon as cracks. The consequence is that design for fatigue consists of calculations for crack-*growth*.

d - include parameters playing a role in *destructive and non-destructive testing*. The more sophisticated the control of construction methods and materials is, the closer the designer may reduce his margins of safety.

e - consider the whole *"environment"* as *"loading"*, including corrosive action, low temperatures and eventually possibilities of inspection and reparation.

f - incorporate *finite-element calculations* combined with *fracture-mechanics*. For instance, what we like to know is how the stress field at the most critical points depends on the length, depth and orientation of *local cracks*, and what is the influence of combinations of local axial and bending deformations in triaxial stress conditions.

A practical observation is that increasing the accuracy of the best part of an analysis from for instance 90% to 95% often means at least doubling the relevant effort. When the extra quantity of work involved would have been put into weaker parts of the problem, the overall reliability of the design analysis might have been improved a lot more.

Uneconomical and time-consuming approaches occur everywhere in the design procedure. For instance there exist sophisticated fatigue-calculations of which the reliability is not better than that of very straight-forward simple approaches. The main fault in the sophisticated methods is that parameters which cannot be put in statistical figures are either left out from the calculation or - just the opposite - taken into account in a completely "overdone" way. It will be seen that "crack closure" is one example. Another one is, that all kinds of load aspects are considered to be "random", while they are not or only in a weak sense (section 2). Perhaps the main one is the use of Miner's rule for unsteady loading instead of methods for calculating crack growth.

2 *Load aspects*

In many cases load experts present their information as cumulative frequency distributions of double amplitudes of stresses (stress ranges). This has become rather standard practice in shipbuilding for longitudinal bending stresses induced by waves with lengths in the order of magnitude of the length of the ship (so-called quasi-static stresses). Vibratory stresses (whipping, springing) at 2-node frequencies are mostly neglected.
In principle both can be estimated from bending moment energy spectra constructed on the basis of energy-spectra of the sea and a response spectrum of the ship (R.A.O.) as determined in towing tanks for different frequencies of regular waves. Mostly the models used are only suitable for recording the quasi-static moments. This needs not be a handicap because the two-node vibratory stresses can be *calculated*, although roughly. For ships this is mostly acceptable, because these vibratory stresses are often second-order stresses. For offshore structures they may become of first order magnitude. Here is a need for improved calculations because up to now the results do not correlate well with data measured on existing structures. The calculated values are on the high side.

For stationary states of the sea the quasi-static stresses (peak-trough) conform well to the Rayleigh-distribution. The Rayleigh-parameter E is equal to 8 times the area of the stress spectrum (E = the R.M.S. of the stress-*ranges*).

Neglecting for a while the vibratory-stresses, we may estimate the cyclic loading of a maritime structure as follows:*

a. Define representative sea-states for the route concerned all over the year with the aid of oceanographer's books.
In order to keep things simple these sea-states are sometimes characterised only by the R.M.S.-values of the wave amplitudes or heights. It will be clear that the *shape* of the wave-spectrum, and particularly the position of the peak relative to the peak(s) of the R.A.O.-spectrum determines to a large extent the resulting stress spectrum. This can be taken into account by introducing first and higher moments of the spectral curves. But the use of one or two standard *shapes* and a few different positions of it in horizontal direction may give sufficiently accurate results. It has no sense to differentiate very far. More important is to dispose of reliable figures about the *probability of occurrence* of the spectra.

b. The multiplication of wave- and R.A.O.-spectra gives stress spectra. Eight times the area of these spectra is equal to the R.M.S.-value of the stress ranges. All R.M.S.-values for the whole life of the structure will have a frequency of occurrence more or less conforming to known statistical distributions (Gauss, Weibull etc.). Then the same is true for the frequency distribution of the stress ranges themselves.

c. So far things have had nothing to do with fatigue.
The commonly made next step is now to use Miner's rule for calculating the fatigue-life ($\sum n/N = 1$).
The first problem then is that the stresses obtained in the foregoing are "nominal stresses". These might be used in fatigue-calculations but only when fatigue-curves (Wöhler, S-N) are available for the joints for which we like to know the fatigue-life. If not, "hot-spot" stresses have to be calculated, or measured at structural models or real structures with strain gauges. Then these stress-values may be used in connection to fatigue-data for butt-welds, fillet-welds etc. to be found in the literature.

One should not have the illusion that the answer obtained has a high accuracy. Sometimes it will be much on the safe side, in others unsafe. The weaknesses are particularly present on the loading side and on the side of the fatigue-life calculation. Yet it is very well possible to improve the calculation process essentially without making it too complicated. The rough rule of Palmgren-Miner can be dismissed and load data and fatigue calculations can be more logically connected in crack *growth* calculations starting from N.D.T.-determined defect lengths. This is not new; many experts all over the world favour that approach. In this method the influence of the sequence of loading can largely be incorporated.

In this connection it should be realized that wave induced stresses are *not* purely random. This becomes clear when representative wave spectra are studied over the year. Heavy storms occur particularly in autumn and winter and less in summer. Temperature stresses change from day to night and are most severe in spring and summer. Also - look-

* For extensive discussions and applications, see f.i. [1] and [2].

ing daily - they depend largely on the position of the sun. In the North Sea storms mostly come from western directions. Contrary to typhoons, they grow gradually in strength and die out similarly. Tide streams are very regular.

Some of these aspects of loading are very low-frequent and as such determine the level of mean stresses. Now in connection to mean stresses the commonly hold opinion is that they hardly need to enter in fatigue-calculations. The argument is that in welded structures there exist residual stresses of yield point magnitude. Due to that the average level of the stresses is supposed to be above zero. (If so, when Miner's rule is used it would be reasonable to take fatigue data obtained for repeated loading (R = 0)).

This line of thinking is more or less right for hypothetical structures subjected to constant amplitude, constant mean-stress loading. But even then it is conservative. For only as long as cracks are small, their tips will be within the residual stress field. At greater lengths they leave that field and propagate under conditions mainly determined by the external loading. Apart from that, the presence of a crack will cause local relief of the residual stresses.

In marine structures the loading is neither constant amplitude nor constant mean stress. Early in the life of a structure stormy weather may occur during which the sum of the cyclic (quasi-static) stresses, vibratory stresses and mean stress may approach the yield point, leading to yielding at "hot spots". This will relieve the residual stresses largely. Moreover when cracks are already present, local yielding at a crack tip creates a zone in which in the unloaded condition compressive stresses are present. On the whole the situation improves drastically. Perhaps most important of all is that in the absence of residual stresses new parts of cracks will be able to *close* during the compressive part of the loading cycles. What this means for the fatigue life is illustrated in Fig. 1. It shows that after crack formation it is no longer the range of the stresses (double amplitude) which is responsible for crack growth, but the *tensile* part of the cycle. This is already valid for cracks of 5 mm in length. It should be realized that extreme compressive loads will only reduce slightly the foregoing favourable influences, just because of the phenomenon of crack closure.

There are other arguments for not neglecting mean stresses, and changes and sequences of these. Figure 2 shows in a simplified form what may happen during 24 hours. Vibratory stresses add to the fatigue-damage in two ways: they increase the *number* of cyclic stresses, and they enlarge appreciably the *range* of the quasi-static stresses. In ships the latter is far more important than the former. In offshore structures it may be different.

3 *Shortcomings of Miner's rule*

In section 2 emphasis has been laid on the non-random character of sea-induced loads - particularly for the aspect of sequence of loads - and on the importance of changes of mean stress which may occur.

The present section will show that when using Miner's rule these influences cannot properly be taken into account.

3.1 *Sequence of loads*

Figure 3 gives an idea of progressive simplifications of service loads. The value of each simplification in connection to fatigue-life predictions will be discussed later. Here the lower part of the figure serves as an introduction to Figs. 4 and 5.

From the viewpoint of Miner's rule Figs. 4a and 4b are identical. They both lead to the same fatigue-damage. But, when crack-growth calculations are carried out, the two "programmes" lead to entirely different results. This can be easily understood from the relation $da/dn = C.(\Delta K)^m$. For repeated loading $\Delta K \approx \sigma\sqrt{\pi a}$ applies (for central through cracks in axially loaded plates). When cracks are absent or very small it also applies to alternating loading, but in case of cracks longer than a few mm's. $\Delta K \approx \frac{\sigma}{2}\sqrt{\pi a}$ should be used as a consequence of crack closure during compression.

When applying first repeated loading (Fig. 4a) and taking m = 4 (for convenience) we get $da/dn \approx \sigma^4.a^2$. During the following alternating loading, da/dn drops to $(\sigma/2)^4.a^2$. During this stage a will be larger than during the first stage. But as σ is reduced to $\sigma/2$, da/dn is much smaller than during the first phase.

When the experiment starts with alternating loading (Fig. 4b), there is hardly any crack closure effect because initially there is no crack. Thus $da/dn \approx \sigma^4 a^2$ as for repeated loading.

For one actual case the calculations resulted in: → 3 + 15 = 18 mm (Fig. 4a)
→ 3 + 26 = 29 mm (Fig. 4b).

3.2 *Influence of shifts of mean stress*

Another case which is not accounted for in calculations with Miner's rule is shown in Fig. 5a,b. When σ_1 is below the fatigue limit of the structure concerned, it does not give rise to crack extension ($\sigma_1\sqrt{\pi a}$ < K fatigue limit; a = initial defect length). So, in the situation of Fig. 5a crack growth can only take place when σ_2 is working ($\sigma_2\sqrt{\pi a}$ > K fatigue limit).

In Fig. 5b, σ_2 causes the same amount of cracking as in Fig. 5a. But after that σ_1 may add to the crack extension. This will be so when $\sigma_1\sqrt{\pi(\text{defect + crack})}$ is greater than K fatigue limit.

3.3 *Influence of yield point*

Generally, the yield point of a steel does not play a role in fatigue calculations. This is justified for welded structures under constant amplitude loading. For, for Fe 410 and Fe 510 the yield points are in the proportion of 1 to 1,5, while the fatigue strengths at 10^5 and 10^6 cycles relate only as 1:(1,10 to 1,15); [3], (constant-amplitude; R = -1).

Figure 6 gives results for specimens of Fe 410 and Fe 510 (St. 42, St. 52), subjected to (high) repeated and alternating constant loads and to a programme as indicated. All results correspond to a testing-time of 50 000 cycles. They support well the foregoing discussions under 3.1, 3.2 and 3.3.

1e. The *initiation* of cracks is only governed by the double amplitude of stress and not by the stress ratio R (data for 1 mm crack length).

2e. The yield point of the steel has little effect on the conventional fatigue strengths (R = 0; R = -1), both for the initiation period as for the propagation stage. But the effect of yield point is large in case of regular shifts of the mean. Miner's prediction is very optimistic for Fe 410 and pessimistic for Fe 510 (see 3e).

3e. The influence of shifts of the mean is large and contrary to Miner's hypothesis, (Fe 410: 150 N/mm²; Fe 510: 250 N/mm²; Miner: 200 N/mm²).

4e. Sequence effects are important. For, when all groups of 1000 repeated loads would have been brought together into one group of 25 000 cycles followed by a similar one for alternating loading, the differences in fatigue stress for both steels would be less spectacular.

4 *Possible fatigue-calculation methods*

4.1 *Use of R.M.S.-stress values*

A logical first approach is to see whether it is possible to use the load data more or less in the form they came forward from the before described analysis, viz. as R.M.S.-values of double amplitudes of stresses for short periods (f.i. 12 hours). Indeed, it would be most welcome when the fatigue-damage caused by a short-term packet of varying sea-induced loads would be equivalent to the damage caused by constant amplitude loading with the same number of cycles and a double amplitude equal to the R.M.S. of the ranges (Fig. 3). Apart from the obvious advantages of simplicity and time-saving, this approach includes the cycles of small amplitude (below the fatigue-limit) which become effective in connection to crack propagation above certain lengths. On the other hand the few high peaks of the spectrum, of which the influence is rather beneficial than damaging, are excluded. Paris proposed such a procedure already in 1962 [4]. Swanson et al. have found a favourable support from experiments [5]. Others, like Schijve [6] are not enthusiastic. Figure 7 indicates that some value like $1,2\sqrt{E}$ might be more logical than $\sqrt{E}$. This has an enormous effect on the calculated fatigue-life. (It will be seen later that even higher constants are required). When Q is known, calculations of crack *lengths* with $da/dn = C(\Delta K)^m$ will certainly give more reliable results than Miner's rule. For, sequence effects and *changes* of mean stresses can now be taken into account. It should be realized that the method may lead to far too optimistic results when data for different weather conditions are mixed. Then the Rayleigh distribution no longer applies. But this is not the worst. As stormy periods are far less frequent than periods of better weather, mixing of the data will lead to the complete elimination of the high stresses occurring during storms. This can best be understood by considering a frequency distribution of stress-amplitudes like the one in Fig. 8 from [1]. It may be read as a line which indicates how often specific stresses (ranges) have been exceeded in the period concerned. It may also be used as a histogram. When we look at the interval 10^3 to 10^4 cycles, a value of 35 N/mm² has been exceeded 10^4 times and 50 N/mm² 10^3 times. Consequently there were $10^4 - 10^3 = 9000$ cycles lying between 35 and 50 N/mm². Roughly said, there were 9000 cycles of on the average 42,5 N/mm². But taking into account that the horizontal scale is logarithmic, there were 9000 cycles of on the average 37,5 N/mm². However, from the viewpoint of fatigue crack propagation (and fatigue damage), the stress values for the interval 1000 - 2000 cycles (close to 50 N/mm²) are about three times as effective as the stress values for the interval 9000 - 10 000 cycles (close to 35 N/mm²). (This follows from $da/da = C.(\sigma\sqrt{a})^m$. For m = 3 is $(50/35)^3 \simeq 3$).

When a corresponding correction is made, the representative stress value is 40 N/mm² instead of 37,5 N/mm². Obviously the error becomes smaller the smaller the intervals of N be.

A possible - and not so bad - way of doing fatigue calculations could be by taking blocks of:

9	cycles	of	80 N/mm²	
90	"	"	68 "	
900	"	"	55 "	(Table 1)
9000	"	"	40 "	
90 000	"	"	26 "	
600 000	"	"	8 N/mm²	

and carrying out crack-growth calculations with these values. Even this simple method will yield more reliable results than can be obtained with Miner's rule.

Completely *wrong* would be an approach in which all data are mixed. The 600 000 cycles of 8 N/mm² and the 90 000 of 26 N/mm² would dominate all the other values, even when the R.M.S. or a higher power for the stress values is taken.

$$
\begin{aligned}
600\,000 \times 8^2 &= 384 \times 10^5\\
90\,000 \times 26^2 &= 610 \times 10^5\\
9000 \times 40^2 &= 144 \times 10^5\\
900 \times 55^2 &= 25 \times 10^5\\
90 \times 68^2 &= 5 \times 10^5\\
9 \times 80^2 &= 0{,}5 \times 10^5\\
\hline
700\,000 \qquad & \quad 1168 \times 10^5
\end{aligned}
\qquad \text{(Table 2)}
$$

$$\text{R.M.S.} = \sqrt{\frac{1168 \times 10^5}{7 \times 10^5}} = 13\ \text{N/mm}^2!$$

700 000 Cycles of 13 N/mm² will give no crack growth at all, even at the "hottest spots". But the high-stress blocks of Table 1 may certainly give crack extension at serious weld defects in areas of high stress concentrations in corrosive circumstances.

It is interesting to compare the "block" method with an approach in which the R.M.S.-values of the records obtained at sea are used. From Fig. 7 in [1] the following information can be drawn:

$\sqrt{E}$ N/mm²	frequency of occurrence	corresponding N
21	23	9000
17,5	90	36 000
14	200	80 000
10,5	340	135 000
7	430	172 000
3,5	300	120 000

(Table 3)

It will be immediately clear that this load-programme consisting of $\sqrt{E}$ stress values and corresponding numbers of cycles, will not lead to any cracking. It is clearly less severe than the "block" programme discussed before (Table 1).

Even when these values would be enlarged in accordance with Fig. 7 (factor 1,2), the result of crack growth calculations would still remain too optimistic. The significant value $\sqrt{2E}$ might be a satisfactory calculation tool. Another possibility worth investigating is a triangular short term distribution as shown in Fig. 3. It has the same R.M.S. as the short term Rayleigh distribution and $\sqrt{3E}$ represents the average of the one tenth highest values. But this triangular distribution is more useful for experimental work than for calculations. For the Rayleigh distributions themselves can very well be used for calculating crack growth. The sequence of the individual stress ranges needs not be completely random, but can be defined in a realistic way. One can go very far by introducing corrections for high tensile loads (based on calculations of crack opening displacement (C.O.D. = $K^2/E.\sigma_y$). Then the influence of yield point is taken into account. But without additional experiments the influence of residual (compressive) stresses due to overloading and of strain hardening in the plastic zone near the crack tip is still difficult to quantify, especially in case of welding stresses. It is the authors' opinion that for practical purposes the adverse influence of the welding stresses may be considered to be compensated for by the local compressive stresses due to high tensile loads and the Elber effect on crack closure [7]. Then only the influence of the high tensile loads on crack closure remains in the calculations. In other words, the influence of large shifts of the mean combined with alternating stormy and calm periods.

4.2 *Acquisition of basic data for crack growth calculations*

Corrosion-fatigue-testing is time-consuming because the frequency of testing should correspond to reality. Haibach [8] mentions that testing time may be reduced by a factor 20 at the maximum by omitting the very small stress values.

Another possibility is testing at higher stress levels than the real ones. But this has also its limitations. Above certain stresses the crack tip moves so fast that the corrosive medium has insufficient time to interact. (But see end of this chapter).

There are other methods for reducing testing time. Instead of S-N curves, da/dN-ΔK curves are constructed. What is needed are *accurate* measurements of crack growth. Then it is possible to precrack a plate at high frequency (say 10 Hz). Next the frequency is lowered to 0,1 Hz or 0,2 Hz and crack growth is observed (C.O.D.-measurements can be of help). After 0,5 mm crack extension the frequency is increased to 10 Hz again for about 2 mm crack growth. Then it is lowered again to 0,1 Hz for another 0,5 mm etc.

When the high-frequency testing is carried out in air, it may even be possible to have a check on crack growth afterwards when studying the crack surface. The combination of all low-frequent data permits the construction of a da/dn-ΔK curve (Fig. 9).

It is often said that high stresses have little effect on corrosion fatigue and that the real need is in the very low-stress region. This is only partly true. Figure 2 illustrates the point. When tests would be carried out at frequencies in the order of magnitude of 0,0001 Hz, a great influence of environment on high-stress cycles might become manifest. It would be interesting to compare these results with tests in which alternatively peaks and long rest-periods occur.

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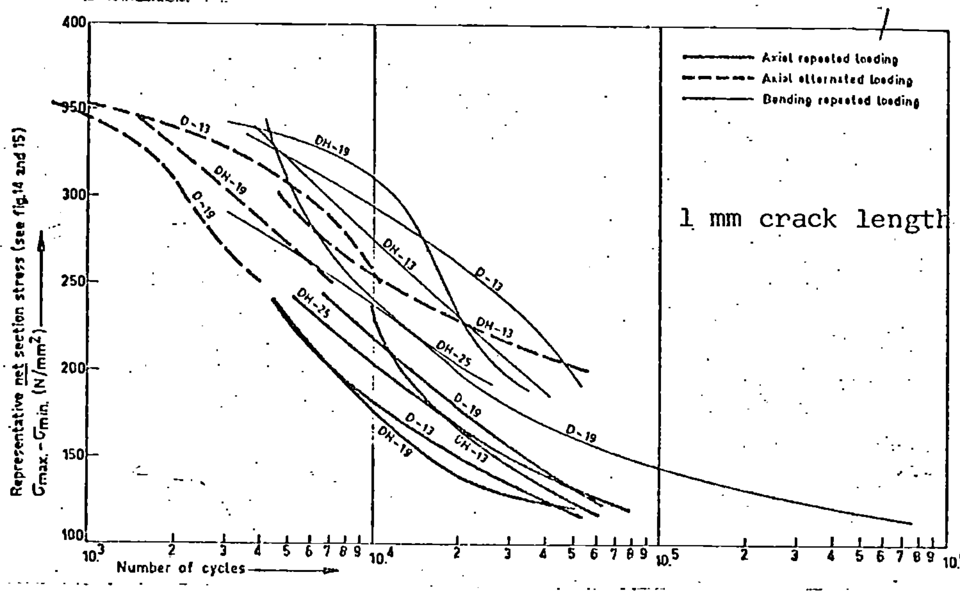


Fig. 1a. No effect of crack closure.

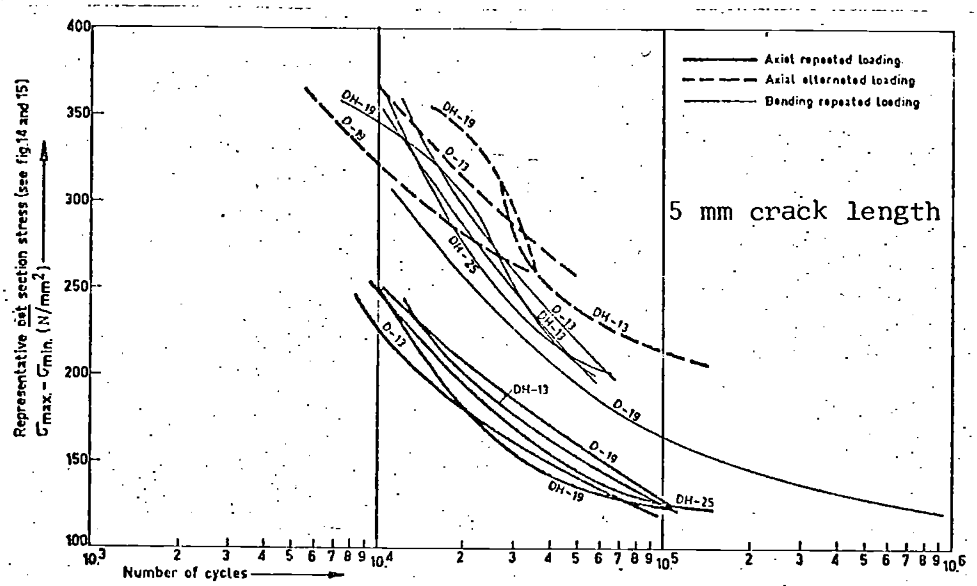


Fig. 1b. Important effect of crack closure.

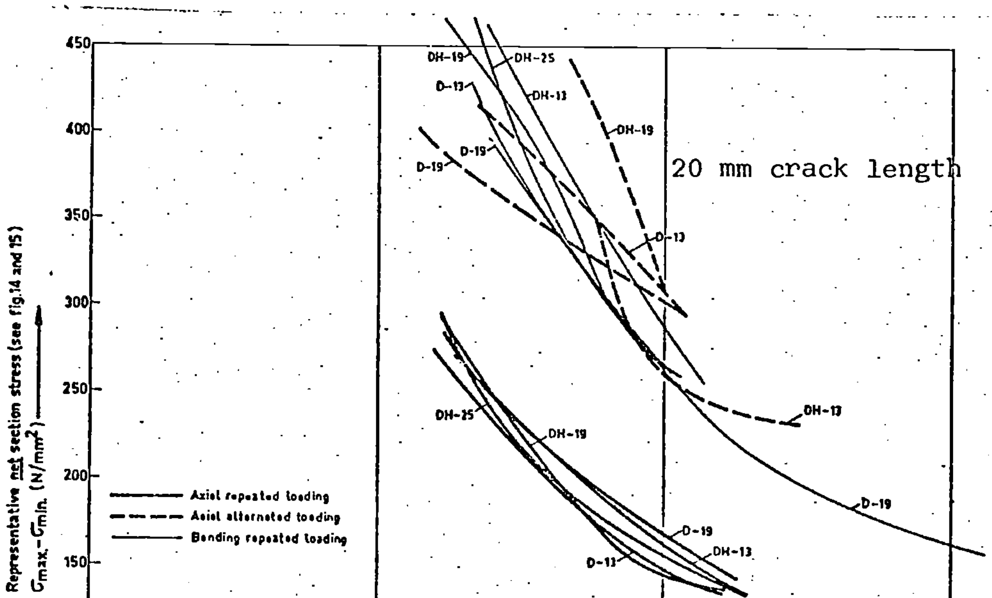


Fig. 1c. Large effect of crack closure.

Fig. 1. Wöhler curves for various crack lengths.
Unwelded notched specimens.

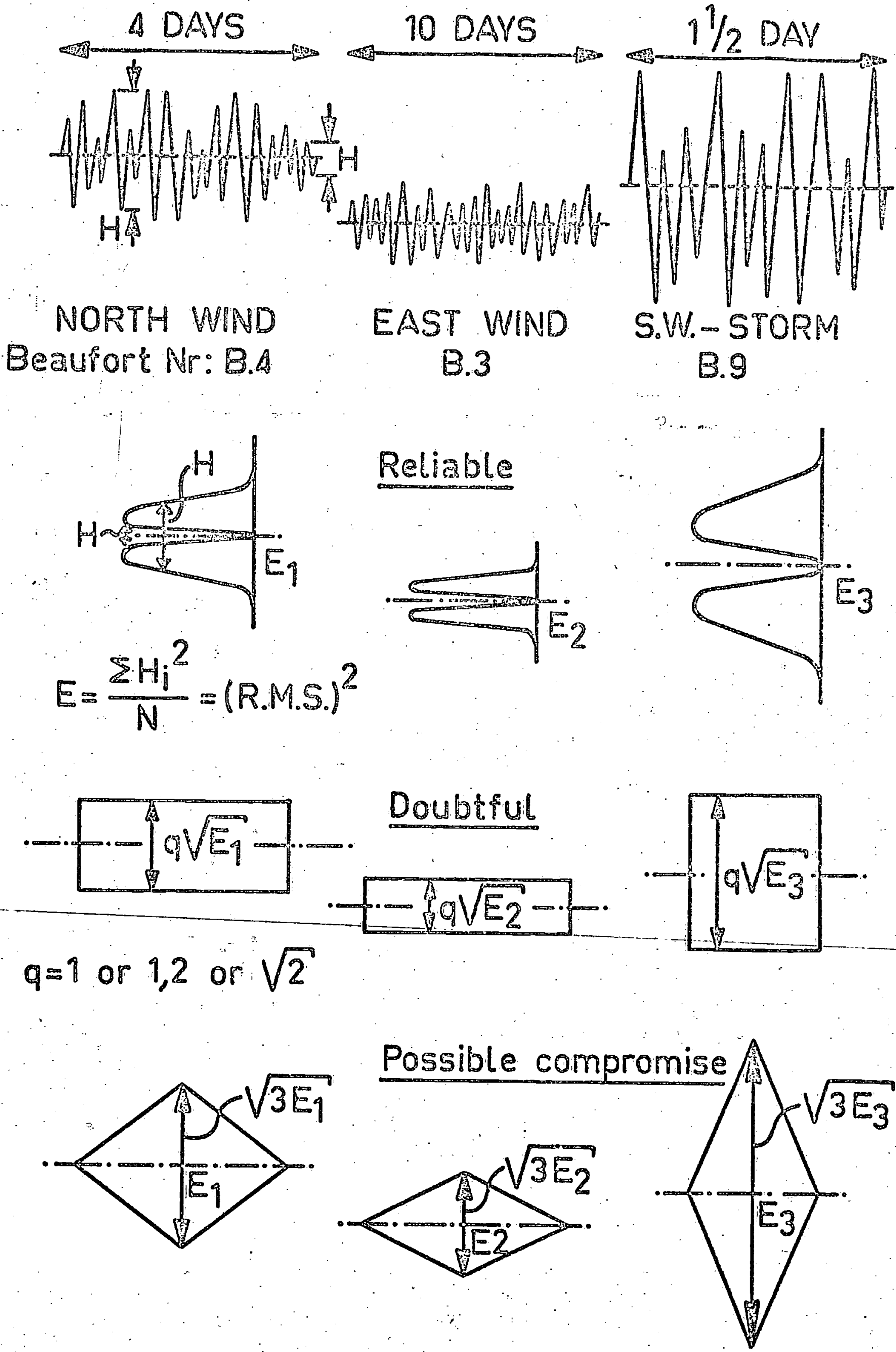


FIG.3 POSSIBLE SIMPLIFICATION OF RECORDS OF WAVE INDUCED STRESSES.

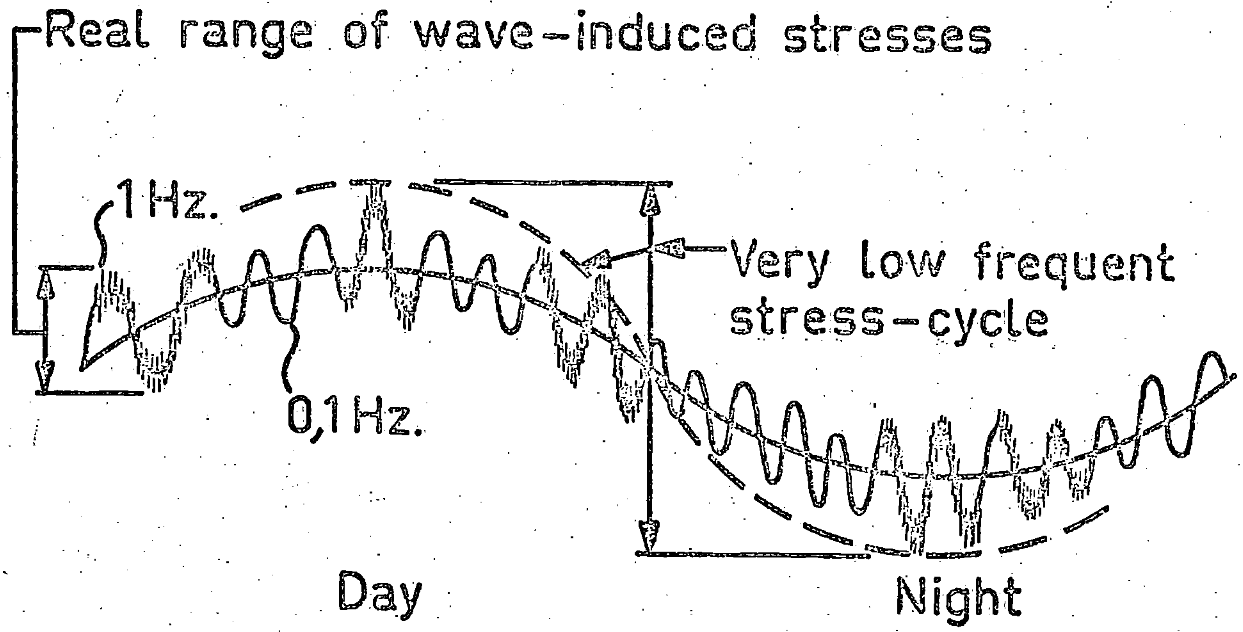


FIG.2 COMPONENTS OF STRESSES.
(quasi-static, wave-rigid body, resonance)

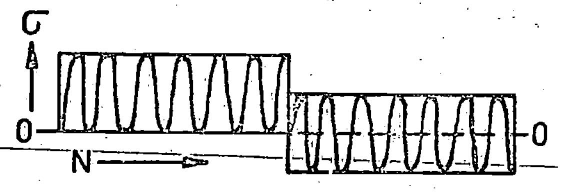


Fig. 4[a]

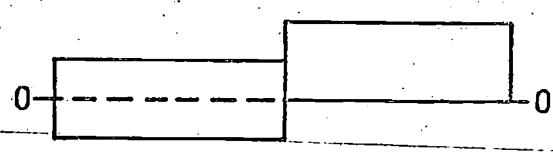


Fig. 4[b]

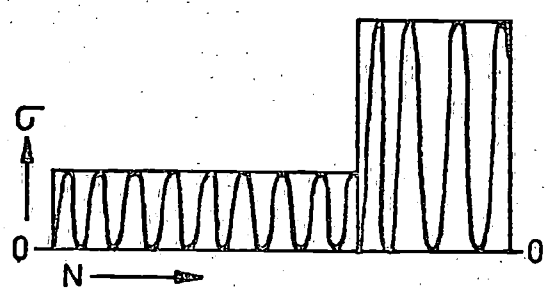


Fig. 5[a]

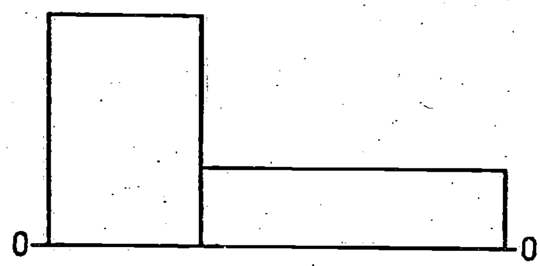


Fig. 5[b]

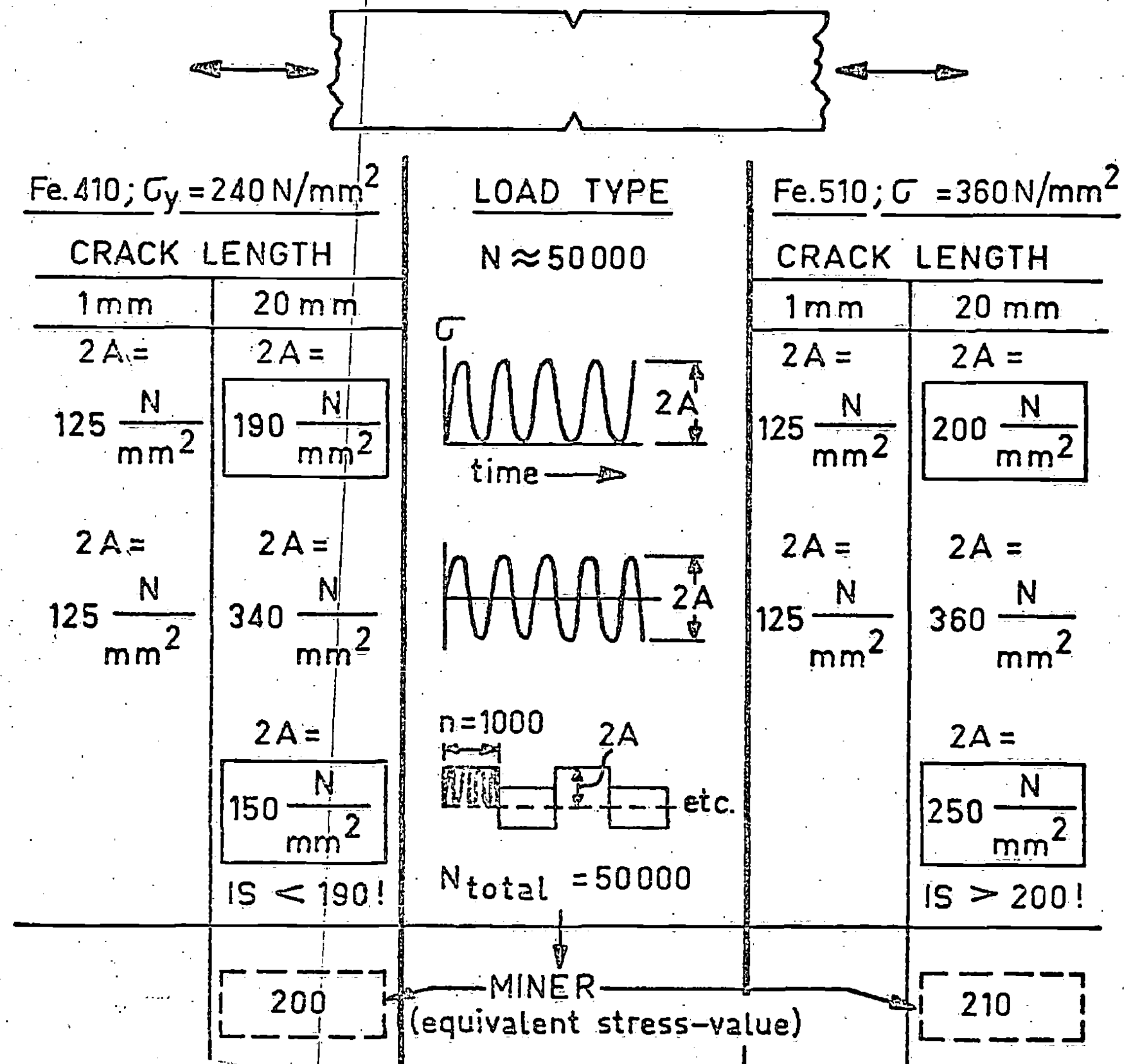
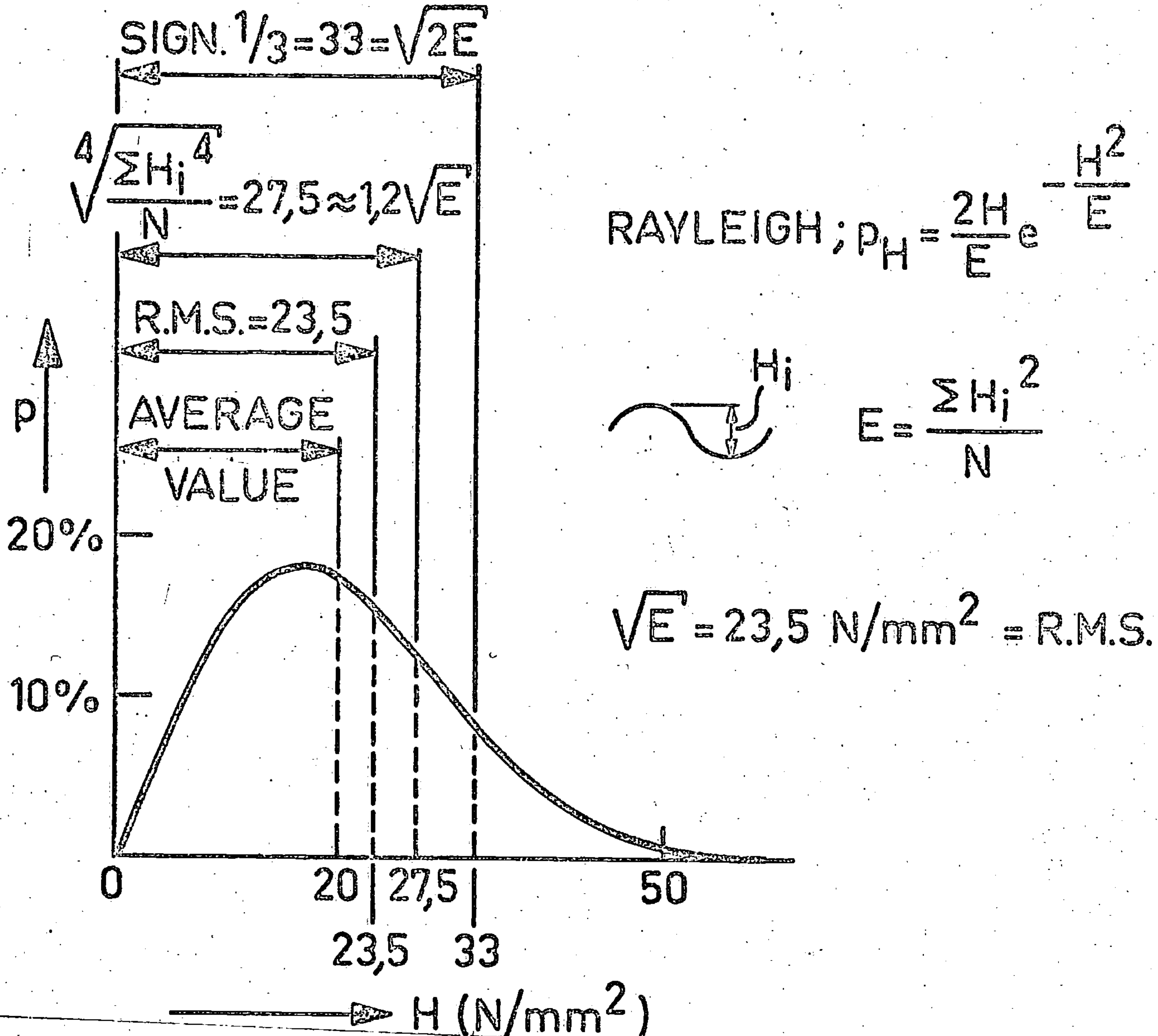


FIG.6 INFLUENCE OF YIELD STRENGTH AND MEAN STRESSES.



THE PARIS FORMULA $\frac{da}{dN} = c(\Delta K)^m = c(\sigma\sqrt{\pi a})^m$ WITH $m=3$ TO 4 INDICATES THAT A R.M.S. STRESS IS NOT SUITABLE FOR REPRESENTING RANDOM LOADING (damage is not proportional to stress squared)

FIG. 7 RAYLEIGH DISTRIBUTION AND REPRESENTATIVE VALUES.

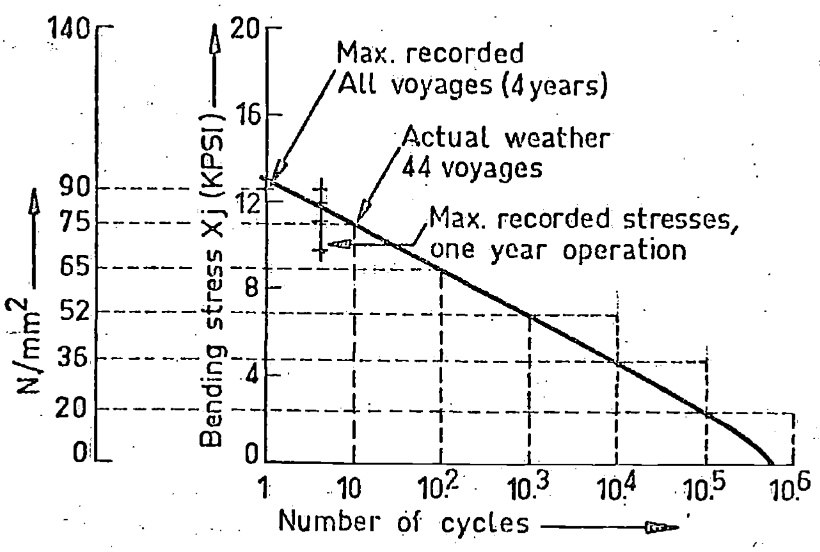


Fig. 8 CUMULATIVE LONG TERM DISTRIBUTION
(S.S. Wolverine State and Hoosier State (Lewis [1])

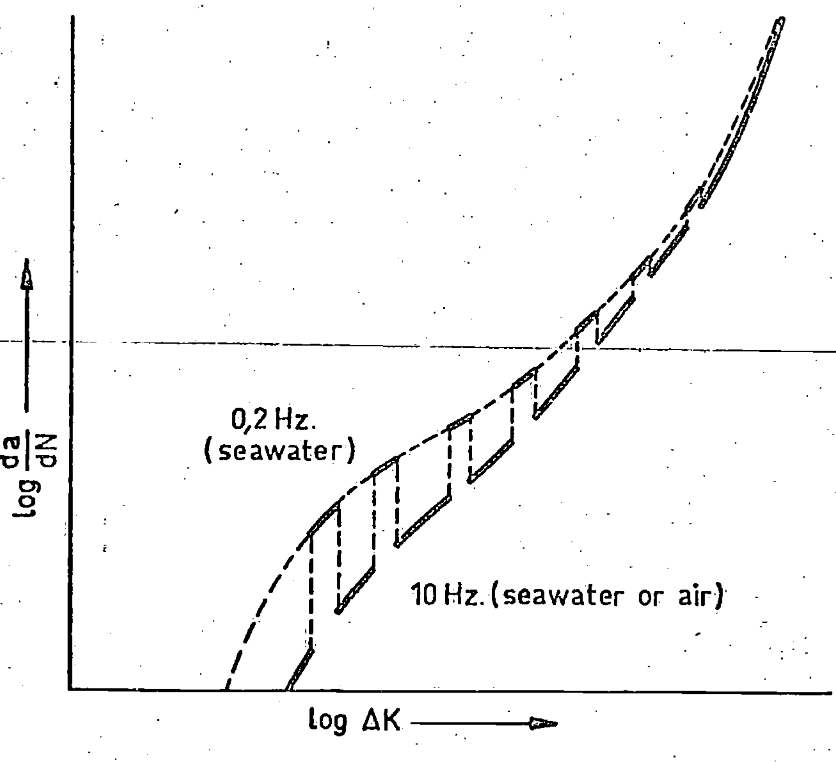


Fig. 9 Accelerated corrosion fatigue testing.