

Fatigue design of offshore steel mono-pile wind substructures

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Offshore wind turbine support structures experience tens of millions of load cycles throughout their design lives, such that these structures are prone to high-cycle fatigue damage. This paper focuses on steel mono-pile substructures, by far the most common type of offshore wind installation, and examines the origin of current fatigue design guidance and what needs to be done to develop guidelines in order to support designers and operators to better optimise offshore wind support structures. The paper discusses some of the incrementally developed techniques for fatigue design from the oil and gas sector and questions whether or not these are entirely appropriate for the rapidly developing offshore wind industry.

Notation

a	$\log_{10} N$ axis intercept
D	diameter measured to the mid-thickness of the shell
d	number of standard deviations below the mean
L	length of transition in thickness
m	slope of the S – N curve
N	number of cycles to failure
r	weld toe radius
S	applied stress range/fatigue strength
S_B	fatigue strength of the joint using the basic S – N curve
t, T	actual thickness of the member (section 1 or section 2 thickness respectively)
t_B	reference thickness
δ_m	$0.15t$ or maximum 3–4 mm
δ_t	shift in neutral axis at thickness transition ($=0.5(T-t)$)
σ	standard deviation of $\log_{10} N$

1. Introduction

Steel mono-pile wind turbine foundations in significant quantities have been and will continue to be installed offshore. With a considerable body of service information now becoming available, it would seem prudent at this juncture to assess

this information and original design methodologies so that future installations can be optimised further and allow the informed development of mitigation measures against localised corrosion and cracking. Serious consideration is also being given to steel jackets and related structural configurations for larger turbines in deeper waters, but there is a deficit of up-to-date relevant information to support the cost-effective optimal design of such structures.

This paper critically analyses the background of current fatigue design guidance and standards for offshore wind support structures and objectively makes the case for updated, more appropriate materials data so that such guidance and standards can be made more relevant to the materials and structures being used today. The new research contribution of this paper is to draw together the relevant factors that have contributed to the design guidance we have today and to illustrate, in several ways, that this needs to be updated and based on contemporary understanding, materials and fabrication processes.

Design of the first generation of steel offshore mono-pile and tubular joint steel structures has been based largely on oil and gas standards and guidance that are, for the greater part, now several decades old. In this intervening period, materials, fabrication technologies and inspection and design techniques have evolved significantly and it is considered that fatigue tests on contemporary materials using representative manufacturing

techniques and exposed to relevant environments and loading would yield important information to support informed decisions concerning future installations and the operation/repair of existing structures.

The following sections review the $S-N$ curves used today and explain their origins. The paper then examines the use of these design rules specifically in the context of steel mono-pile structures whose design limitations are largely dominated by the fatigue lives of their girth-welded sections within the substructure.

2. Background to current fatigue design guidance

In 1976, Gurney published $S-N$ curves for various joint classes based on statistical analyses of experimental data obtained under tensile loading (Gurney, 1976). He suggested that these new curves should be simpler to use than those in BS 153 (BSI, 1972) as the stress analysis was greatly simplified. From statistical analysis it was assumed that the mean $S-N$ curve could be represented by straight lines of slopes -4.0 for class B, -3.5 for class C and -3.0 for all others. The confidence limits of the $S-N$ curves are hyperbolae, but it was assumed that the confidence limits could be represented by straight lines tangential to the hyperbolae and parallel to the mean $S-N$ curve. The resulting design $S-N$ curves are defined by

$$1. \quad \log_{10} N = \log_{10} a - d\sigma + m \log_{10} S$$

where S is the applied stress range, N is the number of cycles to failure under constant amplitude loading conditions, σ is the standard deviation of $\log_{10} N$, d is the number of standard deviations below the mean, m is the slope and a is the $\log_{10} N$ axis intercept.

BS 5400-10 for the fatigue assessment of bridge parts subject to repeated fluctuations of stress incorporated these new $S-N$ curves in 1980 (BSI, 1980). Thirteen years later, they were also applied into BS 7608 for the fatigue design and assessment of steel structures (BSI, 1993). In this document, a correction on stress range for joints of thickness greater than 16 mm for welded joints or 25 mm for bolt diameters was included for the first time. The thickness correction is of the form

$$2. \quad S = S_B \left(\frac{t_B}{t} \right)^{1/4}$$

where S is the fatigue strength of the joint under consideration, S_B is the fatigue strength of the joint using the basic $S-N$ curve, t is the actual thickness of the member and t_B is the reference thickness.

Over the years, various incremental amendments and modifications were applied, with Haagenen (2011) concluding that

DNV-RP-C203 (DNV, 2012) gives the most comprehensive coverage of life assessment methods for air and seawater for the fatigue design of offshore structures.

3. Origin of the corrosion curves

Lotsberg and Larsen (2001) report that the fatigue life of joints with cathodic protection in a seawater environment is not shorter than that for joints in air for $N > 10^7$ cycles. For free corrosion, it is assumed that curves have a constant slope of 3 without any cut-off level. Furthermore, Maddox (1993) stated that the fatigue life obtained in seawater without cathodic protection for $N < 10^6$ cycles is reduced by a factor of approximately 3 compared to that obtained in air. This observation has become enshrined in all subsequent design guidance.

It should be noted that the current fatigue design curves for free corrosion are not based on tests, as might be expected, but on the assumption that the fatigue life under free corrosion is one-third of that for air or cathodically protected members for fatigue lives of less than 10 million cycles. It may be argued that the free-corrosion condition is not designed for, and this lack of rigour is therefore acceptable. However, there may be occasions where operators experience unforeseen situations that require better understanding of likely behaviour that is not represented by the current corrosion fatigue design curves. The authors suggest that the real effect of corrosion on fatigue resistance might be as shown in Figure 1 (i.e. an increasing effect over with time), but this has never been demonstrated in a test programme.

4. The thickness correction

A decrease in fatigue strength for thicker joints is a generally accepted phenomenon. However, the scale of the decrease and the reasons for the shorter lives for thicker joints are still the subject of some controversy. Berge (1985) argued that the decrease is primarily caused by the increased local weld toe stresses caused by the change in weld geometry of thicker joints. The stress concentration of a weld depends on the plate thickness T and the local weld toe radius r – the ratio will be lower if thickness is increased, therefore, also the stress concentration (see Figure 2).

Other reasons for the lower fatigue resistance of thicker sections for the same stress range are known as

- the ‘volumetric effect’, where simply having more material implies a greater likelihood of having more defects (in the case of weldments this is even more likely where there will be more weld runs, etc.) and
- the ‘stress gradient effect’ – this applies to thin sections under bending and the associated stress gradient, which has

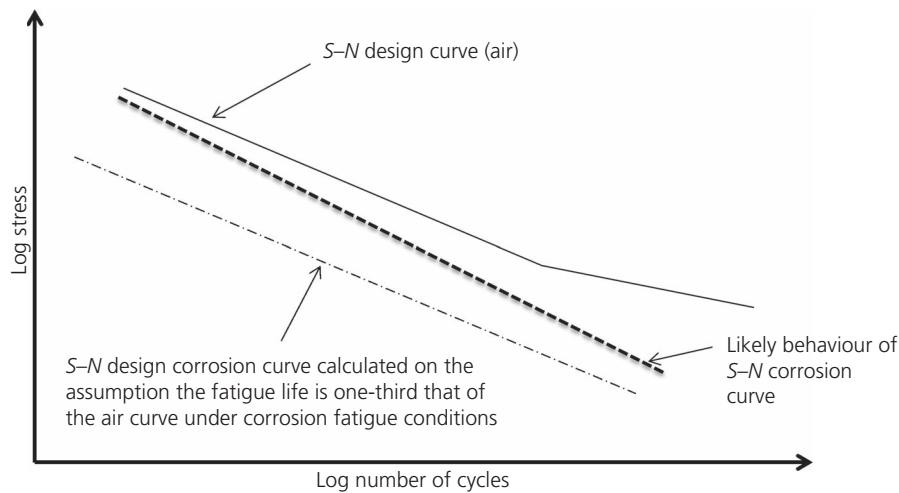


Figure 1. Likely corrosion $S-N$ fatigue behaviour compared with current design guidance

been shown to make thinner sections relatively strong against fatigue.

However, for large-diameter mono-piles, neither of these two effects is relevant and, as the weld toe radius tends to infinity for ground-flush welds, the argument to apply a thickness correction for ground-flush and low stress concentration factor (SCF) joints is particularly weak. The reason for applying the thickness effect for the class C curve is therefore due to the ‘volumetric effect’ (i.e. an increased occurrence of internal defects).

For illustrative purposes, comparing design guidance for class C curves (double-sided ground-flush welds), the percentage reduction in life is stark since BS 7608 (BSI, 1993) does not

require the application of a thickness correction for this kind of weld. For example, if the DNV C1 curve (DNV, 2012) is considered in order to estimate the fatigue life of an 87 mm thick member for a stress range of 50 MPa, there will be a life reduction of 87.64% compared to the British Standard C curve which, using the same time basis as above (cyclic frequency), is approximately 214 years (see Figure 3). This figure used a 25 mm reference thickness and exponent of 0.15 (note that a thickness reference of 32 mm applies to tubular joint chord-brace details). Here, the thickness correction was applied to modify the $S-N$ curve for illustrative purposes where, normally, a thickness correction applied in design is to raise the effective stress range. The thickness correction effect becomes greater the further the deviation from the reference thickness (i.e. 25 mm in this case).

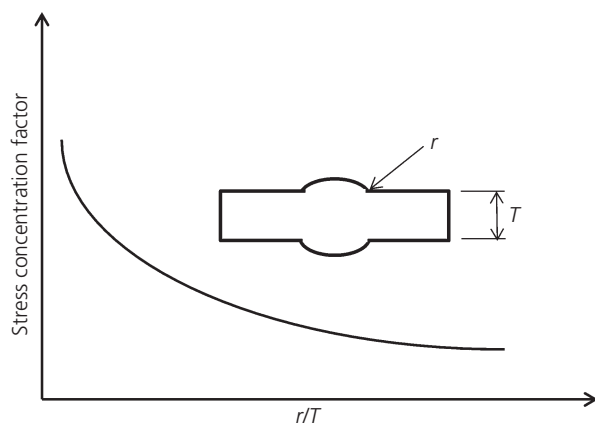


Figure 2. Stress concentration dependence on plate thickness T and local weld toe radius r

5. $S-N$ curves for mono-piles

It is simply not possible to test full-scale sections and there is evidence that the choice of fatigue test specimen is critical to the resulting $S-N$ curve. Specimens need to be as representative as possible of the true situation, but an informed decision needs to be made concerning ways in which test loads can be reduced. It is accepted that the following aspects are all important for testing

- parent material and welding specification
- plate thickness
- edge effects, if any
- residual stress
- environment
- loading frequency in the case of environmentally assisted cracking.

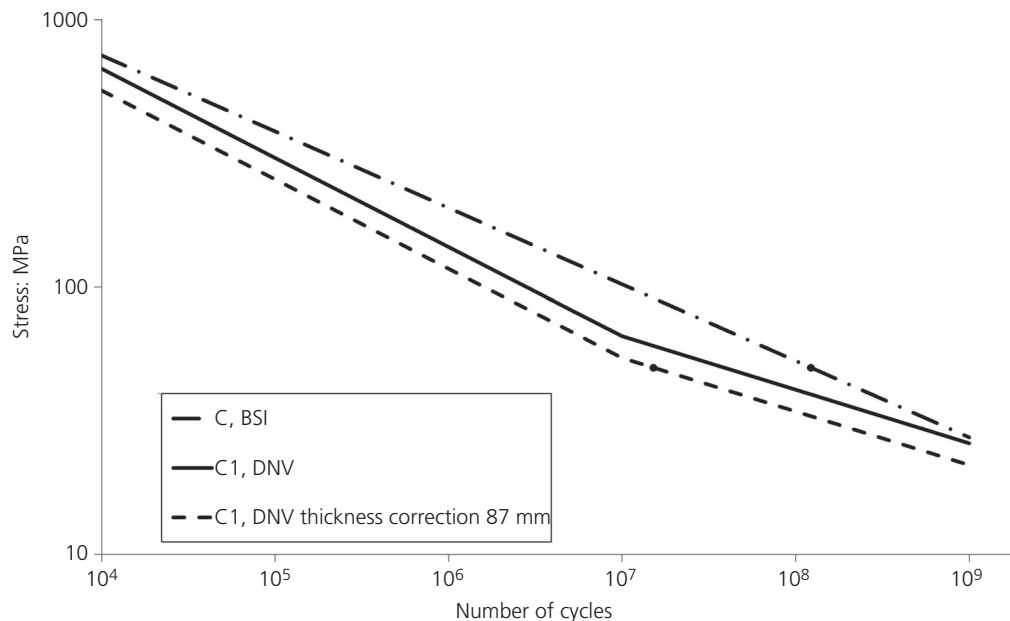


Figure 3. DNV C1 curve (DNV, 2012) and BS 7608 class C curve (BSI, 1993) with and without thickness correction for 87 mm

Zhang *et al.* (2008) reported on a study concerning examination of the ‘C’ curve against a series of tests, stating

In general, establishing the fatigue strengths of the flush-ground butt welds was a challenge. In the absence of weld toes and any significant embedded flaws, fatigue cracking could initiate at various locations in the specimens other than the weld. In the event, a test was terminated for one of four reasons:

1. failure in the weld (11 specimens)
2. failure in the weld but from the edge of the specimen (11 specimens)
3. failure from the machine grips or in parent plate (22 specimens)
4. run-out (i.e. specimen did not fail) (24 specimens).

That is, out of 68 test specimens only 11 could be viewed as valid.

Salama (1999) reported that if strip specimens are tested under a condition that simulates the presence of high residual stresses, such as maintaining the maximum cyclic stress equal to the yield strength, the results could be considered similar to those of pipes (see Figure 4). However, this observation was made on pipe specimens of relatively small diameter and it is not at all clear that the same effects would be seen in large-diameter mono-pile sections.

6. Misalignment stresses

It is generally agreed that significant variation in fatigue strength can be caused by plate and tube misalignment due to vastly increased stress concentrations where there are appreciable amounts of misalignment. Lotsberg (2009) presented a very useful study and review of SCFs due to misalignment of butt welds in plates and girth-welded joints. Lotsberg made reference to the fact that misalignment penalties have been included in fatigue design rules for plated structures (e.g. BSI, 2000; DNV, 2011) for many years. For completeness, the pertinent equations are reproduced here.

The SCF equation for misaligned girth butt welds for tubulars derived by Connelly and Zettlemoyer (1993) is

$$3. \quad SCF = 1 + \frac{2 \cdot 6(\delta_t + \delta_m)}{t} \frac{1}{1 + 0.7(T/t)^{1.4}}$$

where t is the section 1 thickness, T is section 2 thickness, δ_t is the shift in neutral axis at the thickness transition ($=0.5(T-t)$) and δ_m is $0.15t$ or a maximum of 3–4 mm.

Equation 3 was derived from finite-element analyses of tubular sections with a diameter to thickness ratio $D/t=25$ and it was mainly intended to be used for girth welds in tubular members in jacket structures. For larger D/t ratios, Equation 3 provides non-conservative SCFs.

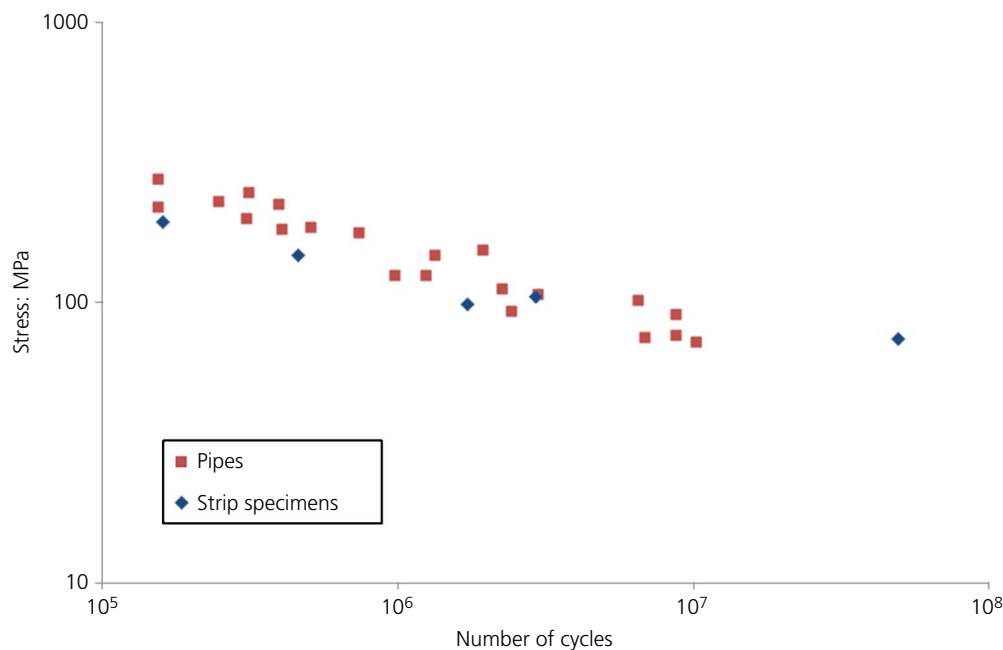


Figure 4. Fatigue test results (after Salama, 1999) on girth-welded pipes and strips machined from pipes

The following equation for SCFs in butt-welded tubulars was derived by Lotsberg (2009) based on shell theory

$$4. \quad SCF = 1 + \frac{6(\delta_t + \delta_m)}{t} \frac{1}{1 + (T/t)^{2.5}} e^{-a}$$

in which L is the length of the transition in thickness, D is the diameter measured to the mid-thickness of the shell and

$$a = \frac{1.82L}{(Dt)^{0.5}} \frac{1}{1 + (T/t)^{2.5}}$$

Equation 4 is reported to provide SCFs in good agreement with numerical analysis results for the low D/t ratios typically used for design of jacket structures, with D/t in the region of 20–40.

Lotsberg (2009) proposed an alternative design equation for SCFs for circumferential butt welds in shell structures and this is now the preferred misalignment SCF equation used for girth-welded joints specified in DNV-RP-C203 (DNV, 2012)

$$5. \quad SCF = 1 + \frac{6(\delta_t + \delta_m)}{t} \frac{1}{1 + (T/t)^\beta} e^{-a}$$

in which

$$a = \frac{1.82L}{(Dt)^{0.5}} \frac{1}{1 + (T/t)^\beta}$$

and

$$\beta = 1.5 - \frac{1}{\log(D/t)} + \frac{3}{[\log(D/t)]^2}$$

Table 1 compares the SCF equations for a 500 mm × 20 mm wall thickness tubular with a 5.2 m × 85 mm wall thickness tubular where $L=t$. This shows that the stress concentration effect of a similar degree of misalignment for a large-diameter tubular such as a wind turbine mono-pile is significantly less than for the smaller diameter oil and gas pipeline geometries. The $S-N$ curves for girth-welded joints are all derived from small-diameter tests and the literature describing these tests does not generally report the manufacturing/misalignment tolerances of the test specimens.

If it is considered that the design curves inherently represent a more severe misalignment SCF, a correction to the curves based on Table 1 yields the results shown in Figure 5. That is, for the DNV C1 air curve, if it is considered that the inherent over-conservatism due to small-diameter tubular misalignment is a factor on stress of 1.072/1.306 (Equation 3) then life prediction at a stress range of 51 MPa would be increased by approximately

D : mm	t : mm	D/t	δ_m : mm	SCF		
				Equation 3	Equation 4	Equation 5
500	20	25.0	4	1.306	1.60	1.60
5200	85	61.2	4	1.072	1.14	1.14

Table 1. Comparison of large- and small-diameter misalignment SCFs

47 years; considering Lotsberg's recommended Equation 5, life prediction would be increased by approximately 125 years.

To clarify, misalignment tolerances should be considered in design by an additional SCF. The point being made here is that the design curves were not formulated as a result of tests conducted on zero misaligned specimens as a baseline. In fact, there is no information available whatsoever concerning the tolerances of the specimens tested and the illustration here is that even if the specimens complied with the same standards as routinely employed today, the relative severity of any misalignment is far greater on smaller diameter specimens than for larger diameter tubes. This is illustrated in Figure 5 as an 'in-built' over-conservatism in the design curves.

It is not proposed that such corrections should be added to design recommendations but merely to illustrate that not only

are the current design curves likely to be conservative due to the fact that they were determined from a series of un-coordinated tests with uncertain quality control provisions, but also that thickness penalties and misalignment models may not be always appropriate and, if this is the case, these most certainly contribute to over-conservatism in fatigue design.

7. Summary and conclusion

This paper has summarised the origins of the most commonly used fatigue design guidance particularly for offshore wind mono-piles. Design guidance and understanding has been transferred from very different oil and gas structural systems using databases and knowledge that are almost 30 years out of date and unrepresentative of current practices, leading, as shown in this paper, to potentially over-engineered solutions. This may be an acceptable compromise for single structures, but costs become unacceptably high for mass production.

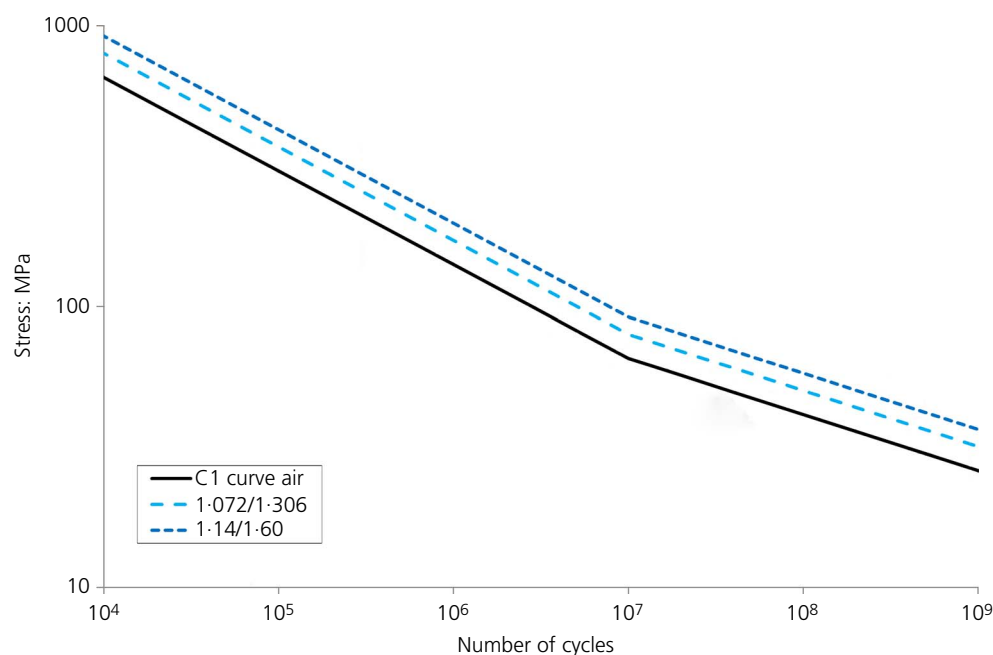


Figure 5. Damage reduction by misalignment on the C1 $S-N$ curve in air

Readers will appreciate that scaling effects in structural fatigue are not linear and can often be counterintuitive. This paper draws particular attention to thickness effects, misalignment and corrosion but, inherent in the use of ageing standards is an absolute failure to benefit from advances in inspection, quality control, beneficial surface technologies and contemporary fabrication and joining techniques developed over the past two decades. Fundamentally, corrosion fatigue is an empirical discipline and understanding of its behaviour cannot progress or develop in the absence of testing.

Offshore wind developers and operators are now realising that existing standards and codes are not streamlined for structural optimisation and, by implication, cost optimisation. It is suggested that prioritising research efforts in this field will greatly reduce the enormity of the structural engineering challenges ahead and allow both the industry and the research community to embrace modern manufacturing and fabrication advances that currently remain elusive given the lack of fundamental research to understand the capacity in this area.

There is an absolute need for what remains of the offshore structures research community to work with designers, certification authorities and the industry in a coordinated fashion to develop understanding that is fit for the purpose of ensuring cost-efficient and reliable marine structural systems to ensure that the future of this sector continues to be led by those countries currently 'pioneering' this technology.

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