

Article

Numerical Prediction of the Impact of Residual Stresses on the Fatigue Life of Welded S355 Steel Plates Using a Phase-Field Approach

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Abstract

The welding process used in the manufacture of offshore wind turbine foundations generates significant residual stresses, which can strongly influence fatigue performance. A better understanding of their magnitude, distribution and relaxation is therefore important for improving fatigue life predictions. This study investigates the influence of welding residual stresses on the fatigue resistance of offshore wind turbine monopile structures, with particular focus on S355 double V-groove multi-pass butt welds. A two-stage finite element procedure was used to replicate the welding process, with a thermal model first predicting the temperature dissipation for each weld pass, followed by a mechanical model to determine the resulting residual stress field. This field was then introduced as an initial condition within a phase-field fatigue model incorporating several empirical residual stress relaxation relationships. The phase-field model was calibrated against the SLIC as-welded mean S-N curve. The results showed that, in the absence of relaxation, welding residual stresses reduced the predicted fatigue life by a factor of approximately 4 to 8, depending on the applied stress range. When residual stress relaxation was included, the damaging effect was reduced and the predicted S-N slope became dependent on the selected relaxation formulation. Removing the residual stress field after back-calibration increased the predicted fatigue life by up to approximately four times. The effectiveness of representing residual stresses through an equivalent R -ratio was also assessed, with $R = 0.5$ providing a conservative approximation, while values closer to $R = 0.25$ gave better agreement when residual stress relaxation was considered.

Keywords: residual stress; fatigue life; welded joints; offshore wind; phase-fieldAcademic Editors: Gabriele Milani,
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1. Introduction

Offshore wind energy has been a crucial component in the fight to decarbonise the electricity grid due to the scalability, energy independence and power production capabilities. As the technology has matured, the relative Levelized Cost of Electricity (LCOE) of offshore wind energy has decreased significantly, in some cases outperforming that of traditional fossil fuels. Life extension has become an attractive means of reducing the LCOE, provided that the structural integrity of existing foundations can be reliably reassessed. The dominant majority of the existing offshore wind turbines feature a monopile-type structure as a foundation, with other minorities being jacket foundations or floating [1]. The monopile foundations require thick structural steel plates to be cold formed, rolled

and double V-groove multi-pass butt-welded, which would create potential areas of vulnerability from which fatigue cracks are likely to initiate and propagate. The likelihood of fatigue cracking is higher in the weld region compared to the base metal for a number of reasons, such as the stress concentration at the weld toe, weld porosity, and residual stresses. The variable nature of the welding procedure and quality leads to a high degree of scatter within the fatigue behaviour. As such, designers often base fatigue life estimates on the fatigue design curve, which represents the mean fit to an S-N fatigue test database for each class of welded detail, minus two standard deviations. A better understanding of the scatter in a fatigue test allows for a more comprehensive review of the design curve, and may assist in safe life extension.

Welding generates self-equilibrating residual stress fields, with tensile stresses typically concentrated within the weld metal (WM) and heat-affected zone (HAZ) and balanced by compressive stresses elsewhere in the plate. Their magnitude may approach the material yield strength and is governed by the non-uniform thermal expansion and contraction occurring during welding. The resulting distribution is sensitive to factors including plate thickness, weld sequencing, heat input and restraint conditions. Experimental studies using contour and neutron diffraction techniques have consistently identified high tensile residual stresses within thick welded structural steel plates, particularly within the weld metal and near the weld toe. Jacob et al. [2] measured longitudinal residual stresses approaching the material yield strength in a 90 mm thick S355 double V-groove weld, such as those found in offshore wind turbine monopile foundations, while investigations across different plate thicknesses indicate that both the magnitude and distribution of these stresses are sensitive to specimen thickness and welding conditions [3,4]. Manufacturing conditions and joint geometry can substantially alter the magnitude and distribution of residual stresses. Taraphdar et al. [5] combined experimental and numerical methods for 40 mm thick high-strength double V-groove butt welds, showing that restraint and groove asymmetry modify the stress field, with unequal grooves reducing peak surface stresses on the smaller-groove side. Woo et al. [6] similarly found that welding temperature altered both the location and magnitude of peak residual stresses in 70 mm thick single V-groove welds, with the higher-temperature condition producing values close to the BM yield strength. Similar process sensitivity has been reported beyond fusion welding: Wu et al. [7] linked peening velocity and coverage to deformation and residual stress development, while Li et al. [8] demonstrated the influence of unequal-thickness spot-weld configurations on deformation and residual stress in vehicle side walls.

Tensile residual stresses can significantly reduce the fatigue life of welded structures since, in the absence of relaxation mechanisms, they will significantly increase the mean stress and subsequently the effective load ratio (i.e., R-ratio). This is why tensile residual stresses are considered detrimental (also known as damaging), whereas compressive residual stresses are generally regarded as beneficial. To overcome the impact of damaging residual stresses, current design standards implement systems to ensure the highly variable residual stresses are accounted for. Among the existing international standards and guidelines, DNV-RP-C203 [9] is the main recommended practice that is widely used in industry for the design of offshore wind turbine welded foundations. DNV-RP-C203 operates with the approach that the residual stresses are embedded within the design curves to maintain a sufficient level of conservatism for design purposes, which is an approach in line with Eurocode 3 [10]. Whilst design standards and guidelines typically recommend fatigue S-N design curves, understanding crack growth rates and flaw assessment are also critical in ensuring structural integrity of offshore wind assets. BS 7910 [11] provides practical guidelines for the life assessment of cracked structures, suggesting that the recommended fatigue crack growth trends associated with effective R-ratios greater than $R > 0.5$ should be

applied to welded structures. This approach provides conservative estimates of remaining life by not accounting for residual stress relaxation due to cyclic loading conditions and crack propagation. In the absence of real-life residual stress values for various welded structures, BS 7910 provides equations for residual stress states to be assumed within or near the weld or recommends the use of finite element analysis (FEA) modelling of the weld procedure.

Numerical studies have increasingly combined predicted or measured residual stress fields with fracture and fatigue analyses. Malschaert et al. [12] identified significant redistribution of residual stresses during crack growth, while Rodenburg et al. [13] demonstrated that specimen cutting and size reduction can release residual stresses and produce non-conservative fatigue life estimates. Jacob et al. [14] combined neutron diffraction measurements with numerical crack-growth modelling for S355 weldments, showing that residual stresses can be particularly influential within the near-threshold regime. At the S–N level, numerical and data-assisted approaches have also been used to separate residual stress effects from other sources of fatigue scatter, although considerable uncertainty remains [15,16].

Whilst it is very well documented that residual stresses can have a significant impact on the lifespan of engineering components and structures, applying a mean stress correction factor may be inadequate to capture the true effects as the residual stress may relax over time, particularly under loading cycles. Across the existing literature, it is a constant theme that the residual stress relaxes in a front-loaded way, i.e., the largest redistribution of residual stress occurs in the first few cycles, as a combination of applied and residual stresses trigger local yielding. It is critical to fully understand this relaxation and the subsequent effect on overall fatigue life, as the local effective R-ratio may vary throughout the load history. Schubnell et al. [17] sought to understand the effect of residual stresses in welded joints of an offshore wind turbine jacket foundation. The study utilised the X-ray diffraction technique to determine the pre-loaded residual stresses, the stress levels of the component with the applied and residual loads, and residuals after 10,000 cycles. The study indicated that, under load, the stress levels seen in the structure are 1.4 to 4 times higher than applied stresses, and after 10,000 load cycles the high levels of tensile residual stresses were reduced. Similarly, Yang et al. [18] used the blind-hole drilling method to measure residual stresses in welded bridge deck specimens and investigated their relaxation during fatigue loading, further demonstrating the importance of residual stress redistribution when interpreting the fatigue behaviour of large-scale welded structures. Additional studies have attempted to reproduce and quantify residual stress relaxation through numerical and FEA-based studies. Wang and Qian [19] similarly utilised X-ray diffraction, in conjunction with FEA modelling, to determine the residual stress relaxation in welded components and their results show that significant stress relaxation occurred after the first cycle, and the extent of which was dependent on the maximum load applied. Other studies have sought to use FEA modelling to capture residual stress relaxation, with the majority showing that the amount of relaxation is dependent on the maximum load applied, and the relaxation occurs primarily in the first cycle, or the first few cycles [20]. Statti et al. [21] conducted a comprehensive review of the existing residual stress relaxation techniques, and performed their own analysis utilising an elastic–plastic simulation of a large butt weld and similarly found that most of the residual stress relaxation occurred in the first cycle. Some other studies have attempted to investigate the effect of residual stress relaxation on the overall fatigue life of components and structures; however, they focussed on residual stresses generated from post-weld treatment, such as shot peening. Mikkola et al. [22] investigated the fatigue life of a transversely attached S700 welded plate, which was treated through High-Frequency Mechanical Impact (HFMI) means, and found

that the benefit of compressive residual stresses in fatigue life decreased as the stress range, stress ratio and peak load magnitude increased. These conclusions are in line with a study conducted by Leguinagoicoa et al. [23] who analysed the residual stresses and fatigue lives of shot-peened specimens. They similarly observed that compressive residual stresses can relax when the applied load exceeds the yield stress in compression. It was also noted that the material exhibits cyclic softening, leading to a reduction in yield stress as the number of loading cycles increases.

Whilst the generation and relaxation of residual stresses due to welding have seen significant attention and provided the research community with great insights into the levels of residual stresses seen, the direct impact of these on the fatigue life presents design challenges. The variability in the residual stress field, relaxation of residual stresses and materials' response to non-zero mean stress effects present increased variations in the fatigue life, the extent of which is not fully understood. The main aim of this study is to investigate the effect that residual stress has on the fatigue life of large-scale welds, such as those found in offshore wind turbine monopile foundations, by developing a better understanding of how residual stresses vary and relax to predict the true fatigue response of the welded structures in the presence and absence of residual stresses, and compare the effectiveness of implicitly accounting for residual stresses through the introduction of an *R*-ratio.

The present study was conducted numerically using a multi-stage Abaqus FEA model, by aiming to replicate the residual stresses and fatigue lives present in the large-scale welded samples fabricated and tested in the Structural Life Cycle Industrial Collaboration (SLIC) joint industry project. The SLIC project conducted a rigorous round-robin test campaign, featuring fatigue tests on large-thickness multi-pass double V-groove butt welds, of which 31 samples were in the as-welded condition. The first stage in the Abaqus FEA model was a thermal simulation of the multi-pass weld, utilising element death and birth to simulate the welding procedure. The second stage replicated this death and birth sequencing with a mechanical model, utilising the thermal inputs from the initial stage to produce the required residual stresses. The final stage was utilising a phase-field model capable of capturing crack initiation and fatigue effects, in conjunction with the priorly predicted residual stresses to analyse their impact on fatigue crack initiation to predict S-N fatigue behaviour in as-welded samples. Furthermore, the impact of relaxing residual stresses was assessed by using empirical methods to relax residual stresses, allowing the model to continue with linear elasticity, which would significantly improve computational efficiency. The phase-field modelling procedure was then repeated, and through an iterative recalibration procedure allowed the resulting S-N curve with residual stresses present to match with the SLIC mean curve, allowing for the prediction of the SLIC mean in the absence of residual stresses. The predicted residual stress-free curve was then given an appropriate *R*-ratio to gauge the effectiveness of implied *R*-ratios at replicating residual stresses.

2. Model Development

2.1. Phase-Field Modelling for Fatigue Fracture

The fatigue simulations in this study are based on an AT1 phase-field formulation, in which the sharp crack surface is replaced by a continuous damage variable, ϕ , varying from $\phi = 0$ in the intact material to $\phi = 1$ in the fully cracked state. The total potential energy is written as the sum of the degraded elastic strain energy and the regularised fracture surface energy, with the crack surface density governed by the length scale parameter. The AT1 formulation was selected because it retains a linear elastic response before damage initiation, which is beneficial for high-cycle fatigue simulations of welded steel components.

The governing equations for the phase-field variable are derived from an energy minimisation principle, whereby the total potential energy of the system, Π , is governed by the elastic strain energy stored due to the applied displacement field, the fracture energy required to create new crack surfaces, and the work done by external loads [24]. This introduces a competition between the elastic energy stored within the body and the energy required for crack growth, consistent with Griffith's fracture theory [25]. To formalise this, consider a body occupying an arbitrary domain $\Omega \subset \mathbb{R}^n$, bounded by $\partial\Omega$, and with an applied traction $\mathbf{T}$ acting on a portion of the boundary $\partial\Omega_T$. The displacement field is denoted by $\mathbf{u}$, with the corresponding small-strain tensor given by $\boldsymbol{\varepsilon} = \frac{1}{2}(\nabla\mathbf{u} + \nabla\mathbf{u}^T)$. To avoid issues of tracking the crack surface Γ it is represented by regularising the sharp crack topology by introducing a phase-field variable ϕ . This allows the discontinuous crack surface to be approximated by a continuous damage field distributed over a finite width, dictated by the length scale parameter, ℓ [26–32]. Within this formulation, the fracture energy can be represented by the crack surface density function $\gamma(\phi, \ell)$ allowing the energy functional to be rewritten with respect to volume, as shown in Equation (1):

$$\Pi = \Pi(\mathbf{u}, \phi) \approx \int_{\Omega} g(\phi)\psi_0(\boldsymbol{\varepsilon})dV + \int_{\Omega} \gamma(\phi, \ell)G_c dV - \int_{\partial\Omega_T} \mathbf{T} \cdot \mathbf{u} dS \quad (1)$$

In the formulation presented by Equation (1), the stored strain energy is modified to be represented by the strain energy of the undamaged material, $\psi_0(\boldsymbol{\varepsilon})$, with a degradation function $g(\phi)$ applied. The degradation function is a continuous and monotonic function, introduced to reduce the stiffness of the material point, along with the ability to store strain energy, as the damage variable grows. As such, the degradation function must allow for the full stiffness when undamaged, $g(0) = 1$, and negligible stiffness when fully cracked, $g(1) \approx 0$, utilising the common Ambrosio–Tortelli 1 (AT1) phase-field model [33]; due to the linear region prior to damage, the degradation takes the form $g(\phi) = (1 - \phi^2)$. Note that negligible stiffness is required to ensure computational convergence. The undamaged elastic strain energy density can be derived through a suitably defined Cauchy stiffness matrix, C_0 , as shown in Equation (2).

$$\psi_0(\boldsymbol{\varepsilon}) = \frac{1}{2} \boldsymbol{\varepsilon} : C_0 : \boldsymbol{\varepsilon} \quad (2)$$

Whilst the degradation function governs the reduction in stiffness and energy storage, the fracture energy must also be reformulated to account for the diffused crack representation. The discrete crack surface is approximated through the crack surface density function $\gamma(\phi, \ell)$, which distributes the energy over a domain. For the AT1 approximation of the phase-field paradigm, the crack surface density function takes the form shown in Equation (3) [34].

$$\gamma(\phi, \ell) = \frac{3}{8\ell} \left(\phi + \ell^2 |\nabla\phi|^2 \right) \quad (3)$$

The crack surface density function smears the crack zone over a diffuse region, the size of which is governed by the length scale parameter ℓ . Physically, ℓ can be taken as a material length scale related to the fracture process zone, dictating the mesh resolution required to adequately capture the crack profile. As the length scale parameter trends to zero, so does the area of the diffuse region. The length scale parameter is often thought of as a material property, related to the fracture toughness (G_c), Young's Modulus E and yield stress σ_y of the material, by way of Equation (4) for the AT1 interpretation.

$$\ell_{AT1} = \frac{3G_c E'}{128\sigma_y^2} \quad (4)$$

Following the reformulation of the energy functional, the governing equations of the phase-field problem are obtained by enforcing the stationary of the total potential energy, with respect to the displacement field $\mathbf{u}$ and the phase-field variable ϕ . This is achieved by taking the energy functional and setting it equal to zero, resulting in a coupled system of equations describing mechanical equilibrium and phase-field evolution. Under quasi-static conditions and in the absence of body forces, this can lead to the strong form of the governing equations, consisting of a balance of linear momentum and the phase-field evolution equation, which are solved simultaneously over the domain Ω .

$$\begin{aligned} \nabla \cdot \boldsymbol{\sigma} &= 0 \\ \frac{3}{4} G_c \left(\frac{1}{2\ell} - \ell \nabla^2 \phi \right) + g'(\phi) \psi_0(\varepsilon) &= 0 \end{aligned} \quad (5)$$

The first equation represents the mechanical equilibrium of the solid, where the divergence of the stress tensor $\boldsymbol{\sigma}$ must vanish. The stress tensor is defined in terms of the degraded material response, such that $\boldsymbol{\sigma} = g(\phi) \boldsymbol{\sigma}_0$ where $\boldsymbol{\sigma}_0$ represents the stress in the undamaged condition. The second equation governs the evolution of the phase-field variable and describes the balance between the fracture energy and the elastic driving force for damage.

The presented phase-field formulation provides a robust framework for modelling brittle fracture; however, it does not inherently account for fatigue damage under cyclic loading. Fatigue fracture occurs whenever cracks initiate and propagate progressively due to repeated loading, typically when the applied stresses are below the critical level required for monotonic fracture. In terms of the phase-field model presented, fatigue fracture occurs when the elastic strain energy stored is below the critical levels required to drive growth in the phase-field parameter. To allow the growth of the phase-field parameter at lower stress ranges, a fatigue degradation function $f(\alpha)$ is introduced that effectively lowers the material point's resistance to fracture, as a suitably defined fatigue history variable α grows. The accurate evolution of the fatigue history variable is critical in ensuring a physically representative fatigue response. In the present formulation, the fatigue history variable follows the approach of Carrara et al. [35], later extended by Golahmar et al. [36], whereby it is defined as the accumulated strain energy experienced at a material point under cyclic loading. To prevent unphysical growth behind the crack tip, the history variable is updated once per cycle such that $\alpha_N = \alpha_{N-1} + \Delta\alpha$, where the increment per cycle is given by

$$\Delta\alpha = \left(\frac{\alpha_{max}}{\alpha_{norm}} \right)^n \left(\frac{1-R}{2} \right)^{2\kappa n} \quad (6)$$

In this expression the exponent n is a calibration parameter governing the inverse slope of the S-N curve produced and κ defines the material's sensitivity to mean stress effects. The term α_{max} represents the maximum strain energy seen by a material point during a cycle, given by $\alpha_{max} = \frac{1}{2} \boldsymbol{\sigma} : \boldsymbol{\varepsilon}$ and finally α_{norm} being the maximum strain energy seen at values of stress/strain corresponding to a stress four times the yield stress of the material. In certain formulations, a Heaviside-type function is incorporated to ensure no fatigue damage occurs at stresses below an endurance limit [30]. The fatigue history variable is used within a fatigue degradation function to decrease the material point's resistance to fracture. This fatigue degradation function is similar to that of the fracture degradation function in that it is a smooth continuous function that acts as a coefficient providing full resistance in the undamaged state, i.e., $g(0) = f(0) = 1$. However, unlike the phase-field parameter, the maximum value for α is infinite; therefore, the fatigue degradation must asymptotically approach zero as $\alpha \rightarrow \infty$, and a commonly adopted form is given by:

$$f(\alpha) = \left(1 - \frac{\alpha}{\alpha + \alpha_T}\right)^2 \quad (7)$$

where α_T is a fitting constant controlling the rate of degradation, and (along with the exponent n) bestowing the model the flexibility to mean the inverse slope and intercept of an experimentally derived S-N curve. The incorporation of the fatigue degradation function into the phase-field formulation is achieved by scaling the fracture energy term within the phase-field evolution equation, as shown in Equation (8). This reduces the effective fracture toughness as cyclic loading proceeds.

$$f(\alpha) \frac{3}{4} G_c \left(\frac{1}{2\ell} - \ell \nabla^2 \phi \right) + g'(\phi) \psi_0(\varepsilon) = 0 \quad (8)$$

Modelling fatigue fracture can be a computationally expensive undertaking, as the high number of cycles requires a large number of iterations to complete. Two methods were introduced to increase the efficiency of the model: a cycle jump technique, and a constant load approach. The cycle jump scheme is based on the work of Cojocaru and Karlsson [37], where a number of consecutive cycles are grouped into a single “super-cycle.” In this implementation, three cycles are computed explicitly, and the evolution of the fatigue history variable is monitored. The fourth cycle is then replaced by a jump in the fatigue history variable, estimated using a second-order Taylor series expansion [38]. The magnitude of the cycle jump is adaptively controlled; when the fatigue evolution is stable and consistent between cycles, larger jumps are permitted, whereas rapid or unstable changes result in a reduction in the jump size to maintain accuracy. A modest maximum allowable jump of 100 cycles was shown to significantly increase computational efficiency whilst maintaining accuracy.

The constant load approach further reduces computational cost by representing each fatigue cycle using a single increment. Instead of applying a full loading and unloading cycle, a constant load equal to the maximum applied load is used. This eliminates the need to resolve intermediate loading steps while still capturing fatigue damage evolution. The approach relies on the fatigue history variable being updated once per cycle, with damage accumulation reducing the material stiffness and consequently altering the structural response under the applied load. In the baseline formulation, the maximum stress $\sigma_{\max}$ is assumed equal to the stress range $\Delta\sigma$; however, the fatigue accumulation law (Equation (6)) incorporates the load ratio R , allowing the effect of non-zero minimum stress to be accounted for through a reduction in the accumulated strain energy.

2.2. Implementation of Residual Stress Fields in Fatigue Damage Analysis

The residual stress implementation was incorporated within an Abaqus UEL framework following the phase-field finite element approach reported by Kristensen and Martínez-Pañeda [30]. In the present study, the formulation was extended to read a pre-determined residual stress field at the beginning of the simulation, with element-wise residual stress components assigned in the S11, S22, S33 and S12 directions. The models featured a consistent mesh throughout the modelling procedure, allowing for direct transfer of residual stresses into the phase-field formulation. These residual stresses were then superimposed onto the mechanical elastic stresses, giving the total stress as shown in Equation (9).

$$\sigma_{Tot} = \sigma_{elastic} + \sigma_{RS} \quad (9)$$

This total stress value is then utilised within the equilibrium equations; therefore, the residual stresses behave as an initial stress, modifying both the mean stresses and the maximum stress experienced per cycle. The fatigue model operates by increasing the

fatigue accumulation parameter, which is based on the total strain energy seen by the material point throughout the life of the component. The fatigue accumulation parameter is updated once per cycle at the peak of the loading phase.

Modelling fatigue can be computationally challenging due to the high number of cycles required and resultant increments in the modelling process. To overcome this shortcoming, the employed UEL subroutine utilises multiple methods to increase computational efficiency, such as the cycle-jumping scheme, and the constant load approach. Within the constant load approach, it is assumed that the minimum stress value is zero, i.e., the R -ratio is nought, allowing the model to directly input the maximum simulated strain energy directly, without deducting the minimum strain energy. Problems arise with the introduction of residual stresses to the model, as the maximum stress seen in each cycle is no longer equivalent to the stress range. As shown in Figure 1, the maximum stress is the sum of the residual stresses and the cyclic stresses applied, meaning the model's assumption of cyclic stresses being equal to maximum applied stress is no longer valid. Therefore, an investigation into the consistency of this approach was conducted.

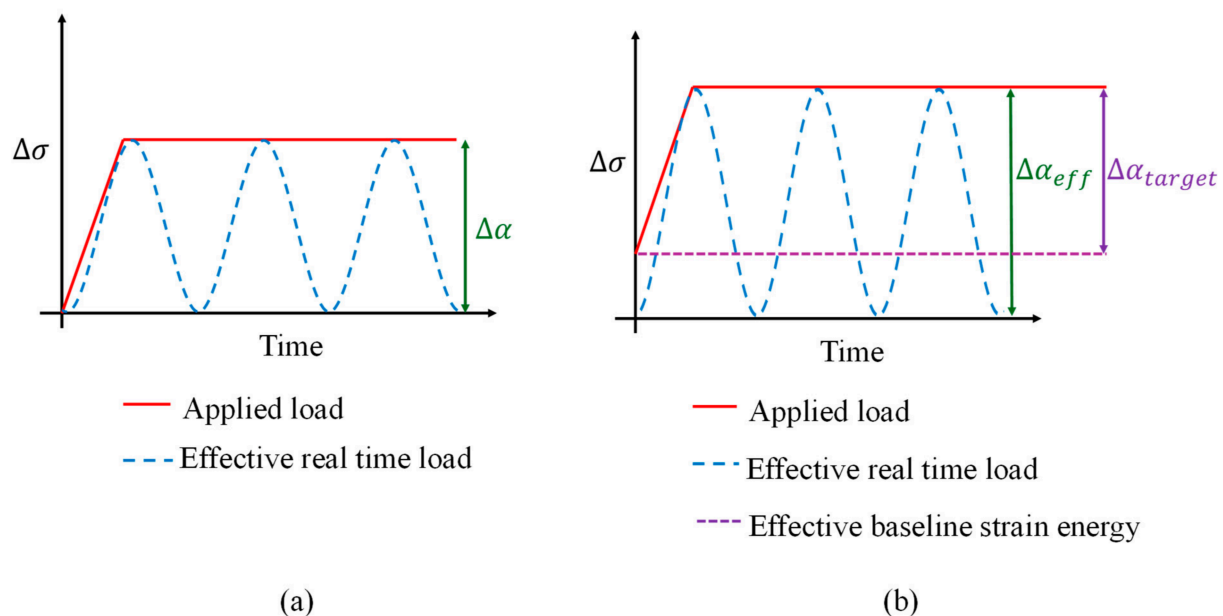


Figure 1. Constant load approach showing (a) accurate representation of applied load at $R = 0$ and (b) representing an inaccurate increase in strain energy when $\sigma_{min} > 0$.

For each cycle, the strain history variable increases by the change in strain energy between the maximum and minimum load, as shown below:

$$\Delta\psi^+ = \psi_{max}^+ - \psi_{min}^+ \quad (10)$$

Assuming no strain energy decomposition, the strain energy is calculated as:

$$\psi^+ = \frac{1}{2}\sigma : \epsilon \quad (11)$$

However, this formulation becomes problematic when residual stresses are introduced as the minimum strain energy is no longer zero. As a result, the model overestimates the cyclic strain energy by implicitly including the static contribution from the residual stress field. Due to the increased stresses and strains at the point of maximum load, the ψ_{max}^+ value will read as shown in Equation (12).

$$\psi^+(\sigma_{max}) = \frac{1}{2}(\sigma_{el} + \sigma_{RS}) : (\varepsilon_{el} + \varepsilon_{RS}) \quad (12)$$

which can be expanded as:

$$\psi^+(\sigma_{max}) = \frac{1}{2}[\sigma_{el} : \varepsilon_{el} + \sigma_{RS} : \varepsilon_{el} + \sigma_{el} : \varepsilon_{RS} + \sigma_{RS} : \varepsilon_{RS}] \quad (13)$$

The term $\sigma_{el} : \varepsilon_{el}$ represents the strain energy due purely to the applied cyclic loading, assuming a zero minimum load. The term $\sigma_{RS} : \varepsilon_{RS}$ corresponds to the static residual stress contribution, and should not contribute to the fatigue damage, as it represents the minimum energy state. The two remaining terms, $\sigma_{RS} : \varepsilon_{el}$ and $\sigma_{el} : \varepsilon_{RS}$, form the cross contribution. This represents the higher mean stress resulting from residual stresses, whilst the applied cyclic amplitude remains unchanged. It can be noted that, with linear elasticity, the two components within the cross term are equal, as the elastic strain can be written as $\varepsilon_{el} = S : \sigma_{el}$ by using the compliance S , making the contribution $\psi_{cross}^+ = 2\sigma_{el} : S : \sigma_{RS}$. Therefore, the increase in strain energy per cycle can be written as shown below:

$$\Delta\psi^+ = \frac{1}{2}[\sigma_{el} : S : \sigma_{el} + 2\sigma_{el} : S : \sigma_{RS}] \quad (14)$$

This expression shows that residual stress increases the cyclic strain energy through the mean stress effect only. The same result can be obtained by evaluating the cyclic strain energy using the elastic strain together with an effective stress equal to the elastic stress plus twice the residual stress, as shown below.

$$\Delta\psi^+ = \frac{1}{2}(\sigma_{el} + 2\sigma_{RS}) : \varepsilon_{el} \quad (15)$$

This formulation ensures that only the cyclic contribution to the strain energy is accumulated within the strain energy history variable. Consequently, the updated model increases the strain history variable according to Equation (21).

2.3. Calibration to SLIC As-Welded Geometry

The present study aims to replicate the results of the SLIC as-welded S-N test campaign, in which 31 samples were fatigue-tested under axial loading condition, with two being recorded as run outs. The round-robin test campaign used S355J2 offshore structural steel, featuring 50 mm thick double V-groove multi-pass butt welds, replicating the circumferential welds present in offshore wind turbine monopile foundations. The samples were fatigue-tested by loading in the direction perpendicular to the welding line, as shown in Figure 2, with the stress range $\Delta\sigma$ ranging from 100 to 300 MPa. Figure 2 further shows the dimensions of the simulated test specimen, with central V-grooves and a weld toe extending by <5 mm, producing a weld toe angle > 150°, as per the requirements of DNV-RP-C203 [9]. The simulations featured quadrilateral elements in the plane strain condition with a critical element size of $h = 0.05$ mm at the initiation point of the weld toe, with a global element size of 0.5 mm. The critical element size was determined through the empirical relationship of $h < 0.2\ell$ adopted by Kristensen et al. [30] and confirmed through a mesh sensitivity analysis. The coupled equations were solved in Abaqus/Standard using a quasi-Newton solution procedure, with the solution kernel reformed after 25 iterations. Default Abaqus/Standard force and displacement convergence tolerances were employed.

The mean fatigue S-N curve produced by the SLIC as-welded campaign featured an inverse slope of $m = -3.37$ and an intercept $\text{Log}C_0 = 13.210$ [39]. The phase-field model allows for specific calibration with existing S-N curves by modifying the exponent n and the fitting parameter α_T . With the geometry shown in Figure 2, and the material properties

from the SLIC test campaign (taken from [40] and summarised in Table 1), the fitting parameters were determined through an iterative procedure resulting in a value of $n = 1.1$ and $\alpha_T = 475$. The resultant simulation reproduced the SLIC as-welded mean curve to an appropriate degree of accuracy ($<1\%$ error) as shown in Figure 3, and acted as the baseline simulation from which only the residual stresses were altered. It should be noted that the calibrated curve does not apply any residual stress as the SLIC campaign utilised welded dogbone specimens without any post-weld heat treatment, meaning the mean curve is inclusive of existing residual stresses. Furthermore, the curve is calibrated using an R -ratio of $R = 0.1$ to replicate that of the SLIC test campaign, and using the nominal stress range in the bulk of the material.

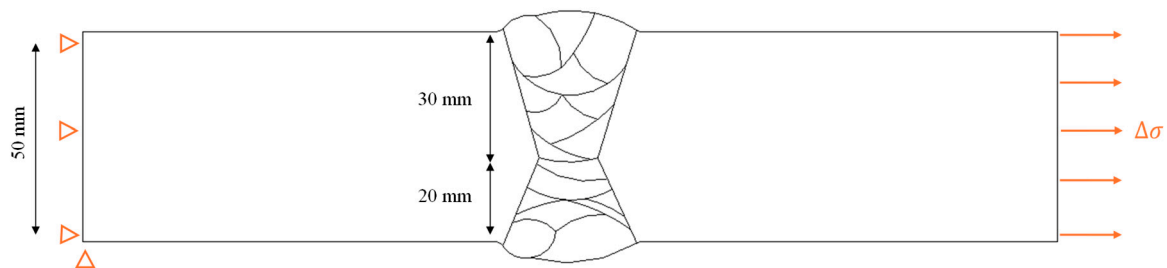


Figure 2. Boundary conditions and dimensions of simulated test specimen, replicating the SLIC as-welded test campaign.

Table 1. Material properties of the SLIC test campaign, as utilised for the present study, taken from [40].

Material Property	Value
Young's Modulus (GPa)	197
Ultimate tensile strength (MPa)	417
Fracture toughness ($\text{MPa}\sqrt{\text{m}}$)	26
Length scale parameter, ℓ (mm)	0.469

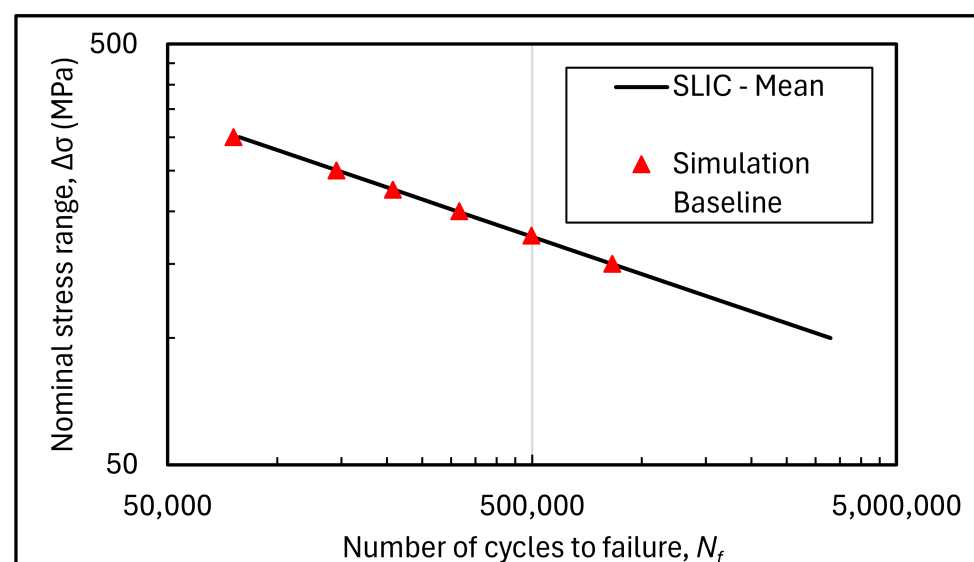


Figure 3. Calibration of the baseline simulation to the SLIC as-welded mean curve.

Hereafter, this calibration is referred to as the baseline case, it does not represent a residual stress-free material response, as the SLIC as-welded mean curve implicitly contains the residual stress state and relaxation experienced by the tested specimens.

2.4. Implementation of Constant Residual Stresses

The baseline simulation was tested using a consistent and constant residual stress profile, such that each element was assigned the same initial residual stress value. In this preliminary analysis, additional levels of constant tensile (i.e., damaging) residual stresses were introduced to the mean S-N curve from the SLIC test campaign. This results in the applied baseline stress field acting in conjunction with the existing residual stress field embedded in the SLIC test specimens in order to evaluate the impact of more conservative assumptions on the predicted fatigue life. This approach may be particularly useful for improving the understanding of fatigue behaviour in monopile structures, especially if future experimental evidence indicates that transverse residual stresses in real offshore wind monopiles are higher than those present in the large-scale SLIC test specimens. Whilst the simulations in this section are not fully representative of real-world conditions, as the internal forces must self-equilibrate in the absence of an external load, they provide an efficient means of assessing the model's sensitivity to residual stress and comparing the resulting trends with the influence of an equivalent representative R -ratio. The input residual stress in the principal loading direction was varied from 50 MPa to 300 MPa to gain insight into low and high mean stress effects, with the corresponding results shown in Figure 4. These results are compared against the R -ratio effect through the reduction metric defined in Equation (6), which is embedded directly within the fatigue accumulation parameter. This reduction metric is based on a Walker-inspired stress correction relationship. The results indicate that the phase-field model, when adjusted to account for residual stress contributions, shows good agreement with the expected influence of the R -ratio predicted by the Walker-based correction, both in terms of the analytical trend [41] and the simulated response. Figure 4 displays the correlation between the predicted results from the Walker-based mean stress correction, and the novel residual stress implementation approach. However, it should be noted that as the residual stress is imposed upon the minimum stress, and independent from the cyclic stress amplitude, the resulting R -ratio varies accordingly.

$$\Delta\sigma_{eq} = \sigma_{max} \left(\frac{1-R}{2} \right)^{\kappa} \quad (16)$$

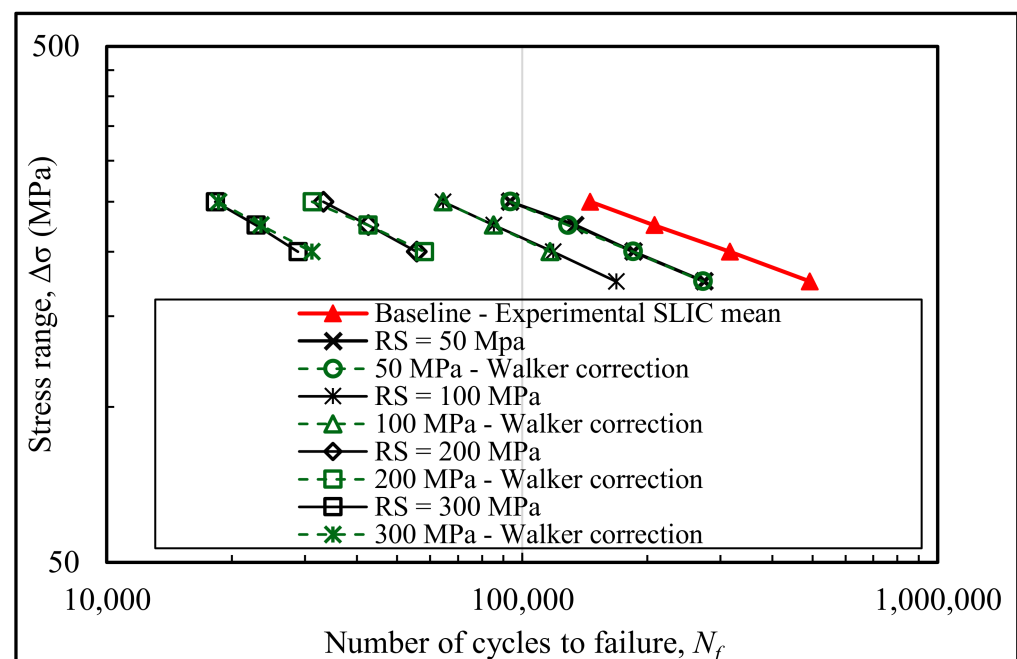


Figure 4. Comparison of constant residual stresses and effective R -ratio using the Walker mean stress correction factor.

3. Prediction of Weld Residual Stress Distribution in the Presence of Relaxation Mechanisms

3.1. Prediction of Weld-Induced Residual Stress Profile

The simplified approach in Section 2.4 provides a result consistent with *R*-ratio effects, using which the residual stresses are treated as constant values which do not redistribute or relax throughout the fatigue life. However, a constant residual stress assumption is rudimentary and insufficient for gaining an accurate representation of reduction in fatigue life for welded structures. Residual stresses in welded joints, such as offshore turbine monopile foundations, feature a complex profile and magnitude distribution, which simplistic models fail to account for. Furthermore, residual stresses are highly variable, due to the complex nature of generation and variances in welding procedure.

To model the residual stresses that are present within a multi-pass double V-groove butt weld, a two-stage approach was conducted using Abaqus Standard, utilising the death and rebirth of selected regions/elements. The first stage involved modelling the temperature using the relevant heat-transfer steps, and the second model utilised the predicted temperatures from stage one to determine the thermal-induced stresses and subsequent residuals remaining after cooling. The model was conducted with the intention of replicating the residual stresses present in the SLIC welded plates. The SLIC project utilised the Submerged Arc-Welding (SAW) technique, a base metal of S355 with a WM of F7A8-EH12K AWS class and Esab OK12.32/SAF Oerlinon OP121TT [40]. In the SLIC project, S355 plates with two different thicknesses of 50 mm and 90 mm were welded for uniaxial fatigue tests and fracture mechanics tests, respectively. For the 90 mm thick plates, over 40 weld passes were performed, compared with 17 weld passes for the 50 mm plates. As the SLIC S-N fatigue data is calibrated using a 50 mm thick plate, the model presented here replicates that geometry, with a double V-groove butt weld featuring 17 weld passes, and the sequencing described in Figure 5.

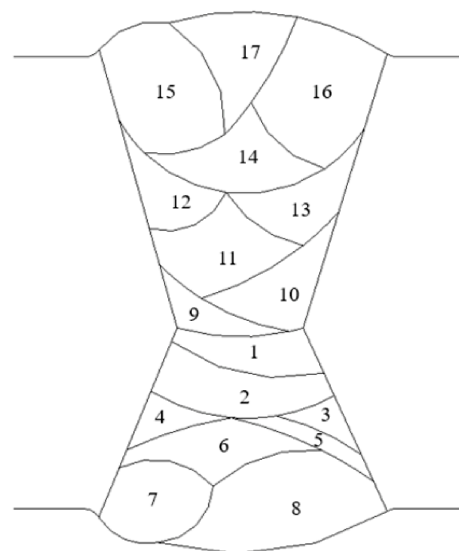


Figure 5. Weld sequencing for FEA simulation on 50 mm thick plates.

For the modelling procedure, both the thermal and mechanical model started with an initial model change step, in which all 17 weld passes were removed (death), with the idea of introducing each pass sequentially. The welded material was given an initial temperature of 1400 °C. Each pass featured three distinct steps, the first of which was a pre-heating step, in which the active edges of the weld pass were heated to a temperature of 1400 °C during a 3 s period. The second step was an insert with a negligibly small time step, in which the model change for the elements included within that weld pass

was deactivated, re-inserting the elements to model (rebirth). The final step introduced is the cooling step, which was chosen as 240 s, allowing the model to cool for each weld pass to an allowable inter-pass temperature of $<225^{\circ}\text{C}$. Longer periods of cooling were allowed between weld passes 8 and 9, accounting for the change in weld direction (3000 s), and after weld pass 17, allowing the model to cool to ambient temperature (8000 s), which was taken as 20°C . For ease of simulation, no distinction in material properties was made between the BM and the WM. The material properties featured temperature-dependent properties for the density, thermal conductivity, specific heat capacity, Young's Modulus, initial yield point, and thermal expansion coefficient, the values of which are summarised in Appendix A [21,42–47]. Additional model and material parameters are shown in Table 2. The boundary conditions were set to match that of the fabrication process, as shown in Figure 6. It should be noted that the boundary condition locking the top weld surface was introduced to replicate a weld used to ensure angular misalignment is limited during the first weld pass; as such, the boundary condition is removed after the cooling stage of the first weld pass.

Table 2. Parameters for thermal model.

Model Parameters	
Absolute Zero	-273
Stefan–Boltzmann Constant	5.67×10^{-14}
Sink/Ambient Temperature	20
Material-specific	
Surface Film Coefficient	2.5×10^5
Radiation Emissivity Coefficient	0.8

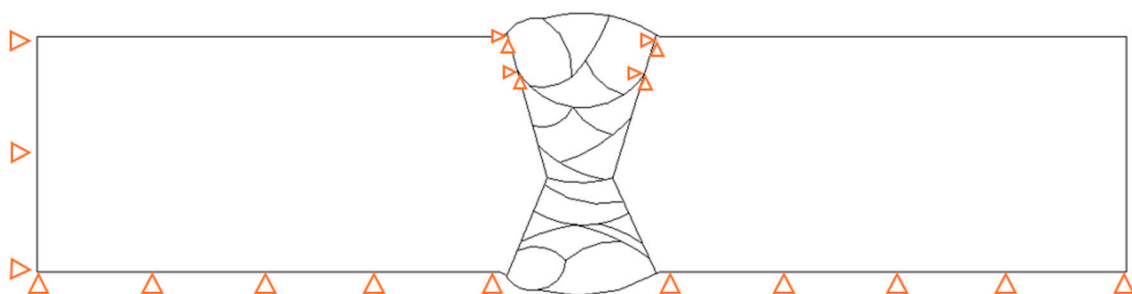


Figure 6. Boundary conditions utilised within thermal and mechanical FEA model.

3.1.1. Reference Results

Residual stresses can be highly variable and are particularly difficult to measure in thick welded plates, due to the limited penetration depth of non-destructive residual stress measurement techniques. As such, limited information is available on the residual stress profile of multi-pass double V-groove butt-welded plates with a thickness of 50 mm and over. The SLIC project utilised the contour method to determine the longitudinal residual stresses in a 90 mm as-welded plate [2] by cutting the specimen, measuring the deformation of the newly created surfaces, and reconstructing the residual stress component through an elastic finite element analysis. The component of residual stresses measured was parallel to the weld direction (i.e., longitudinal component), with the results shown in Figure 7a. Further studies back up the distribution seen in the SLIC 90 mm welded plate, such as Prime et al. [48] who conducted a similar residual stress measurement on a thinner multi-pass butt weld of 37 mm thickness on a ferritic steel BS 4360-Grade 50D, an offshore structural steel broadly comparable to the modern S355 class. Similar to the SLIC project,

the contour method was utilised and the stresses measured in the direction parallel to the weld line. The results, as shown in Figure 7b, demonstrate a similar pattern to that of SLIC measurements, with high (near yield) tensile residual stresses in the centre of each V-groove and a U-shaped ligament between the two high tensile concentrations where the stresses resemble that of the bulk of the BM. Seong et al. [49] researched residual stresses in a single V-groove multi-pass butt weld of 25 mm thick E500 offshore structural steel plates using a variety of residual stress modelling techniques such as the contour method and neutron diffraction technique. The results indicate that the centre of the weld metal has the highest concentration of tensile residual stresses, with compressive residual stresses moving into the bulk of the BM. As shown in Figure 7c, the results were taken at a distance of 12 mm from the free surface of the BM. Unfortunately, likely due to the difficulties in measuring residual stresses of large-scale welded samples, no additional information is available showing the residual stress magnitude or distribution in the transverse or normal directions.

3.1.2. Prediction of Spatial Distribution of Residual Stresses in 50 mm Thick Multi-Pass Butt-Welded S355 Plate

The reference results provided are in the longitudinal direction relative to the weld and unfortunately do not provide a full insight into the distribution and magnitude of stresses in the transverse and normal directions. This is particularly important for offshore wind turbine monopile foundations, where the transverse residual stresses, which are parallel to the applied loading direction on the outer surface of circumferential welds, play a critical role in determining their influence on fatigue life.

Additional sources of uncertainty arise from variations in the welding procedure, material properties, number of weld passes, and plate thickness, all of which can influence both the magnitude and distribution of the residual stresses. Despite these complexities and the limited data available in the literature, the previously reported results do provide a solid baseline upon which the simulation can be benchmarked. Provided the simulated stresses correlate with the measured values and distribution, it is feasible to assume the predicted stresses in the other two directions are representative of the true stress state of an as-welded plate. Figure 8 shows the simulated residual stress field in the longitudinal (parallel to weld line) direction for a 50 mm thick S355 plate, which was used for S-N fatigue testing in the SLIC project. The distribution of stresses correlates well with that predicted by both the SLIC project and Prime et al. with characteristics of high tensile stresses within the centres of each V-groove, and a U-shaped ligament between the hotspots at a stress level akin to that of the bulk of the BM. The values show a slight discrepancy between the measured and predicted values, indicating a likely source of error, with the simplistic material inputs not fully encompassing the changes in temperature or the lack of an annealing point. The residual stresses through a path perpendicular to the weld line, 12 mm away from the free surface for comparison with observations in [49], show a similar pattern to that presented in the literature with a peak at the centre of the weld; however, significant differences are present in the welding procedure, particularly the differing V-groove strategy, with Figure 7c showing the stress patterns from a single V-groove whereas the simulated model utilises a double V-groove weld.

The modelling procedure predicted the residual stress field in multiple directions, providing a complete three-dimensional representation of the weld-induced stress state. Unlike experimental measurements, which are often limited to selected stress components and surface locations, the numerical model captures the spatial distribution of residual stresses throughout the welded joint. These predicted stress components can therefore be directly incorporated into the phase-field fatigue model, enabling the influence of the full multiaxial residual stress field on fatigue crack initiation and propagation to be

evaluated. However, it should be noted that in the absence of residual stress data in the transverse and normal directions, the predicted residual stress field remains an uncertainty, albeit with some confidence provided by correlation in the residual stress field in the longitudinal direction.

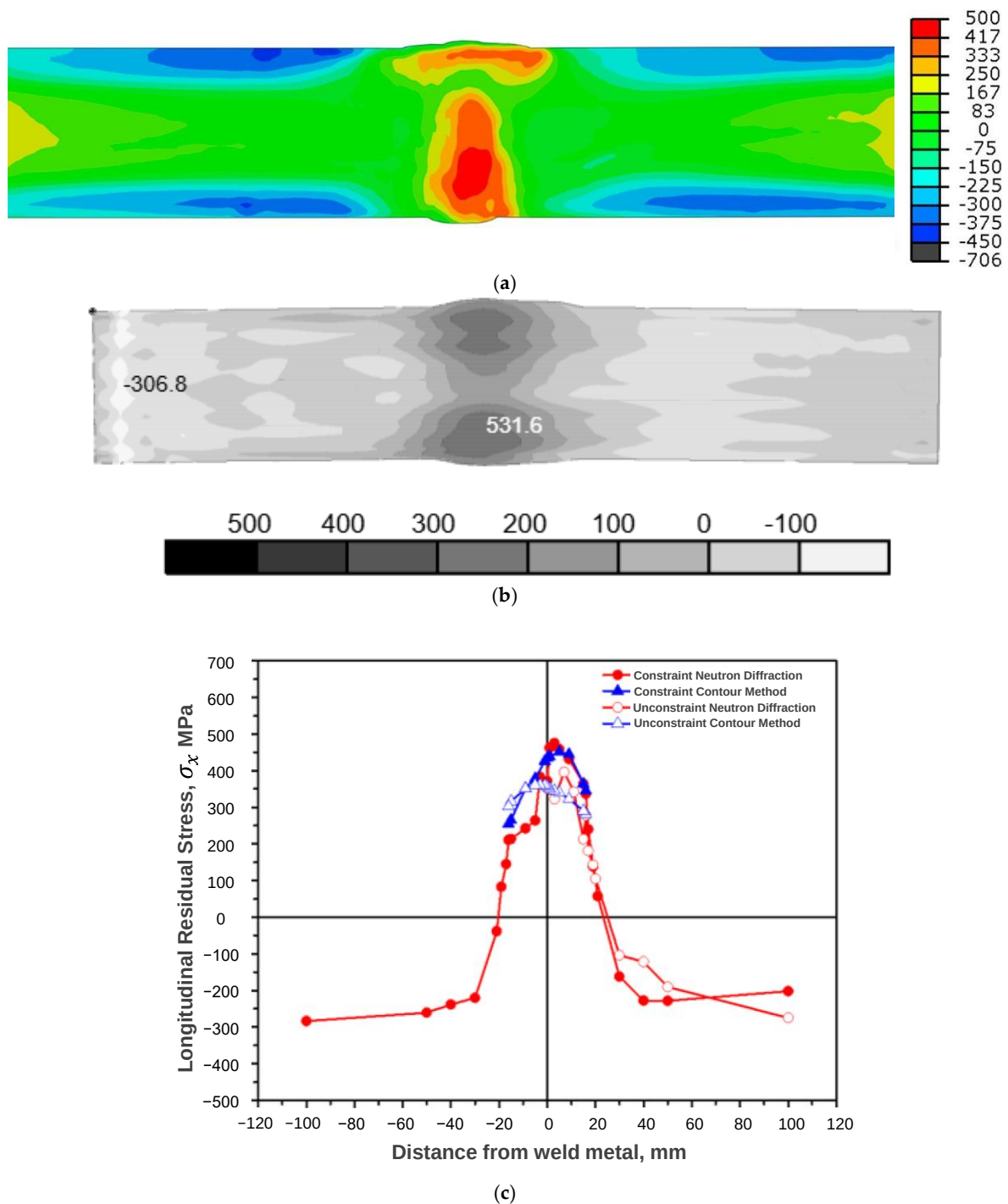


Figure 7. Longitudinal (parallel to weld direction) residual stress distribution of V-groove butt welds: (a) SLIC 90 mm double V-groove S355 plate [2], (b) Prime et al. [48] 37 mm thick double V-groove BS 4360-Grade E50 plate, and (c) Seong et al. [49] 25 mm thick single V-groove E500 plate.

This approach provides a more physically representative assessment of fatigue behaviour than simplified methods that account for residual stresses solely through an equivalent stress ratio (R -ratio). The predicted residual stress fields for 50 mm multi-pass butt-welded S355 plate, which is the geometry employed in uniaxial fatigue testing in the

SLIC project, are presented in Figure 9 for the transverse and normal directions with respect to the weld.

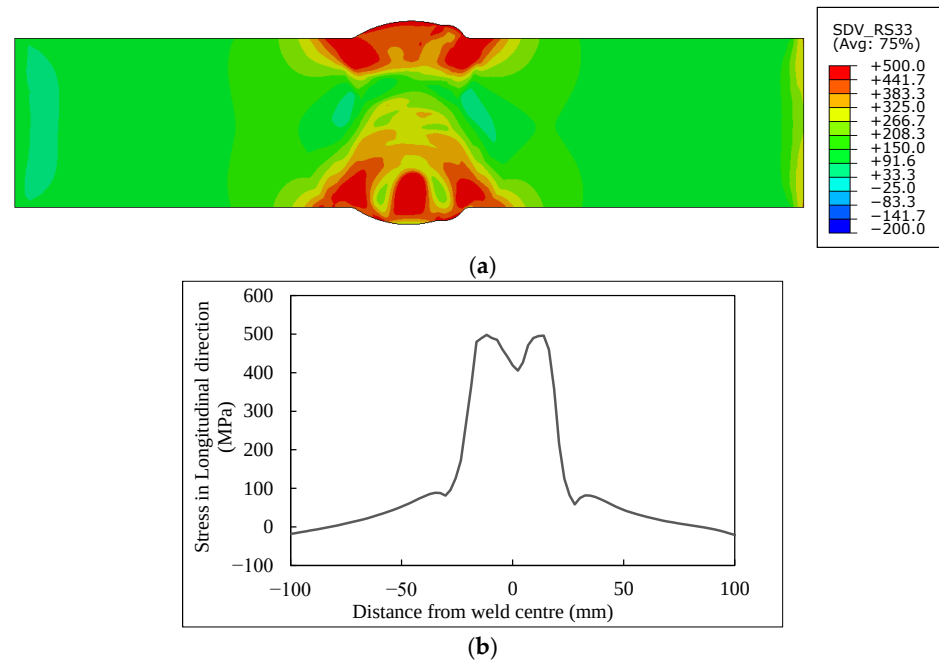


Figure 8. Simulated residual stress results from a 50 mm thick double V-groove multi-pass butt-welded S355 plate: (a) longitudinal residual stresses contour map, (b) longitudinal residual stress profile following a path perpendicular to the weld, and 12 mm from the free surface.

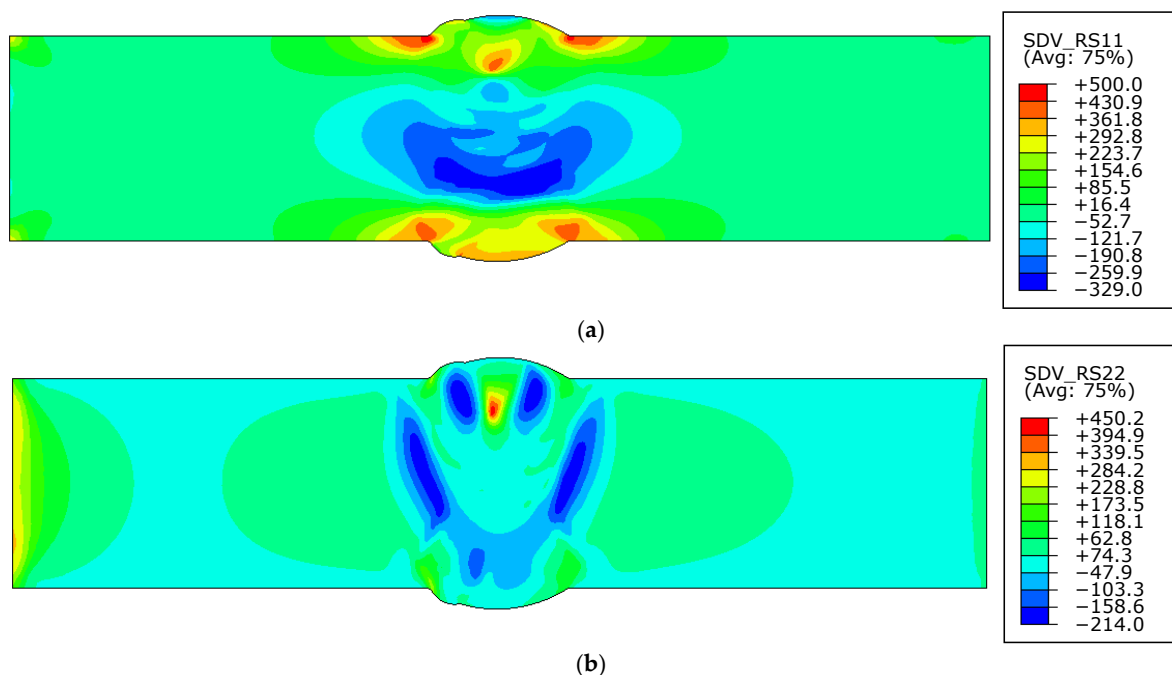


Figure 9. Predicted residual stress profiles for 50 mm multi-pass butt-welded S355 plate for (a) transverse direction, (b) normal direction.

3.2. Residual Stress Relaxation Under Cyclic Loading Condition

Although it is well established that tensile residual stresses introduced during welding can significantly reduce fatigue life, assuming a fixed residual stress profile throughout the entire service life of a welded structure, which is often represented through a constant high R -ratio, may lead to excessive conservatism in fatigue design and life assessment.

This is because under cyclic loading, residual stresses may relax and redistribute, resulting in a stress state that evolves over time and differs from the as-welded post-fabrication condition. Furthermore, in offshore wind applications, monopile foundations are subjected to substantial loading during installation and the piling process, which may further modify or “shake down” the residual stress field compared with the initial post-fabrication state.

Whilst a lot of studies have aimed to mimic the reduction in residual stresses due to applied cyclic loads, very few give quantitative results on how the residual stress relaxation process directly affects the fatigue life. Residual stress relaxation is typically modelled through an elastic–plastic FEA simulation, as it is more physically representative of the relaxation and redistribution process; however, these simulations are significantly more computationally expensive than linear elastic models. As such, the relaxation of residual stresses is often applied through empirical means, allowing for the elastic models to remain in place. There have been multiple studies proposing empirical models for residual stress relaxation, with some formulations based solely on cycle count and others depending on the ratio of maximum stress to yield strength [21]. To gain a more comprehensive understanding of the effect of residual stress redistribution on fatigue life, several of these empirical relaxation relationships were implemented within an Abaqus UEL subroutine, enabling the residual stress field to be updated at each cycle. These existing empirical models are briefly presented and discussed below.

3.2.1. Jhansale & Topper

Jhansale and Topper [50] conducted a study on SAE 4340 which aimed to model the mean stresses in a component as a cyclic load is applied. It was concluded that the mean residual stresses within the component drop radically when the component is subjected to high levels of plastic strain. The study theorised that the drop in mean residual stresses can be modelled through a simplistic power-law, as shown in Equation (17), with the use of a single constant r . In Equation (17), $\sigma_{R,0}$ is the initial residual stresses prior to loading, $\sigma_{R,N}$ is the remaining residual stresses at cycle number N , and r is a constant with values in the range $-0.15 < r < -0.05$.

$$\sigma_{R,N} = \sigma_{R,0} \times N^r \quad (17)$$

3.2.2. Qian et al.

Qian et al. [51] proposed that cyclic relaxation of residual stresses may be analogous to creep deformation. In this approach, the extent of relaxation is expressed as a function of the applied stress amplitude σ_a , material yield stress σ_y and the total number of applied cycles N . With the use of empirically fit material constants a , b , n , and m , Qian et al. introduced a relaxation parameter S , given by

$$S = \left[a \left(\frac{\sigma_a}{\sigma_y} \right)^n + b \right] \times [\log(N + 1)]^m \quad (18)$$

stating that the resulting decrease in residual stresses is given by:

$$\sigma_{R,N} = \sigma_{R,0}(1 - S) \quad (19)$$

A key feature of the model presented by Qian et al. is that the relaxation rate is governed by both the applied stress and the number of cycles, indicating that a higher applied stress will result in more substantial decreases in residual stresses. However, as the model is derived from a creep-based analogy, its accuracy in dealing with lower temperatures with negligible creep can be questioned; therefore, whilst the model provides a flexible description of residual stress relaxation, its use should be considered material-

and condition-dependent. The original fitting constants in the study carried out by Qian et al. were obtained from welded plate experiments using AISI 1008 and AISI 4140 steels with the values adopted shown in Table 3.

Table 3. Fitting constants for the residual stress relaxation of AISI 1008 and AISI 4140 steel utilising the method presented by Qian et al.

Material	<i>m</i>	<i>n</i>	<i>a</i>	<i>b</i>
AISI 1008	0.1	8.0	1.15	0.098
AISI 4140	0.1	3	1.03	0.059

3.2.3. Takanashi et al.

Takanashi et al. [52] developed a logarithmic residual stress relaxation model based on experimental measurements from smooth longitudinal butt welds in structural steels. In the study, the residual stresses were measured near the weld toe and in the longitudinal direction (parallel to weld line). Takanashi et al. proposed the following relationship, giving the residual stresses as a function of the cycle number and normalised by the yield stress of the material, σ_y :

$$\frac{\sigma_{R,N}}{\sigma_y} = A + B \log(N), \quad \text{for } N \leq 10^4 \quad (20)$$

Within the applied formulation, two constants have been introduced: *A*, representing the initial normalised residual stress level, and *B*, controlling the rate of relaxation with increasing cycle numbers. For offshore welded structures, *B* is typically taken as $B \approx 0.1$. Unlike other empirical models, this formulation introduces a maximum cycle count for relaxation of 10,000 cycles, stating that the primary reduction in residual stresses is seen in the early cycles, before the residual stress field stabilises leading to negligible further reductions.

3.2.4. Han et al.

Han et al. [53] proposed a residual stress relaxation model in which the level of relaxation is determined by the superposition of applied stresses and residual stresses. The model is decomposed into two empirical relationships, stating that the relaxation of residual stresses is more significant when the combined stress levels exceed the yield stress of the material. When the combined stress is less than that of the yield, a simplistic formula related only to the number of cycles is applied, similar to the Jhansale and Topper method. However, when the combined stresses exceed the yield stress, the relaxation is dependent on the magnitude of combined stress, relative to the yield, increasing the relaxation to reflect the increased likelihood of local plasticity and stress redistribution. The model was conducted on residual stresses occurring in welded mild steel, reporting that the exponent *k* represents a material property, which, for the mild steel in question, was given as $k = 0.004$.

$$\frac{\sigma_{R,i} + \sigma_a}{\sigma_y} < 1 \Rightarrow \sigma_{R,N} = \sigma_{R,i} \times N^{-k} \quad (21)$$

$$\frac{\sigma_{R,i} + \sigma_a}{\sigma_y} > 1 \Rightarrow \sigma_{R,N} = \sigma_{R,i} \left[-1.6 \frac{\sigma_{R,i} + \sigma_a}{\sigma_y} + 2.6 \right] N^{-k} \quad (22)$$

As the empirical constants adopted in these formulations were calibrated using different steel grades and experimental conditions, the resulting relaxation behaviour should not be interpreted as a material-calibrated prediction for S355. Instead, the models are applied

as comparative scenarios to investigate how different assumptions regarding residual stress relaxation influence the predicted fatigue response.

3.3. Residual Stress Relaxation Effects on Fatigue Life

The empirical formulations described in the previous section vary greatly, with the methods proposed by Jhansale & Topper and Takanashi being consistently applied to the geometry, regardless of stress levels applied, resulting in a uniform reduction in residual stresses. The reverse can be noticed within the Qian and Han models, which are both dependent on stress levels. This dependency on stress levels produces interesting variations in residual stresses as load is applied. Considering the Han model in a one-dimensional single-element case, a residual stress of 200 MPa is prescribed in the loading direction with a material parameter ($k = 0.004$). When a cyclic load with a stress range of 300 MPa is applied, the model predicts significant residual stress relaxation due to the exceedance of the yield-related threshold governing plastic redistribution. In contrast, when the applied stress range is reduced to 200 MPa, the residual stress remains largely unchanged, indicating negligible relaxation under subcritical loading conditions. These trends are illustrated in Figure 10. More generally, each empirical relaxation formulation employs material-specific calibration parameters and distinct assumptions regarding the governing relaxation mechanisms. Figure 11 demonstrates how these calibration parameters influence the predicted evolution of residual stress for a single element, again initialised with a residual stress of 200 MPa and subjected to (a) a stress range of 300 MPa and (b) a stress range of 200 MPa. The results highlight the strong dependence of residual stress relaxation behaviour on both the applied loading level and the chosen empirical model, emphasising the importance of appropriate calibration when predicting fatigue performance.

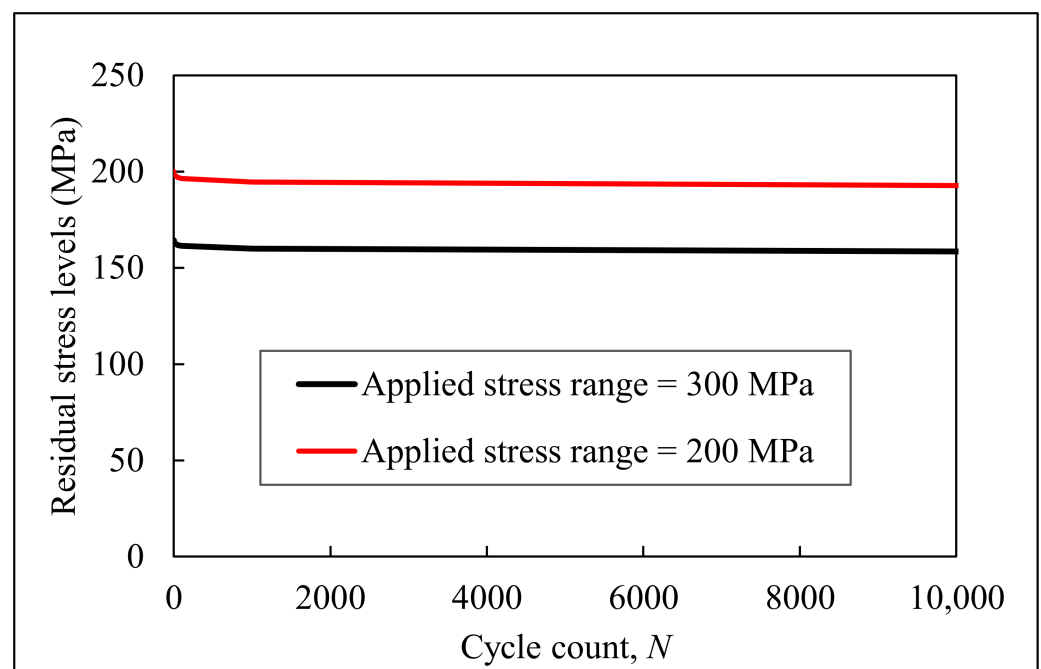


Figure 10. Estimated residual stress relaxation of a single element, subjected to an initial residual stress of 200 MPa, utilising the method applied by Han et al.

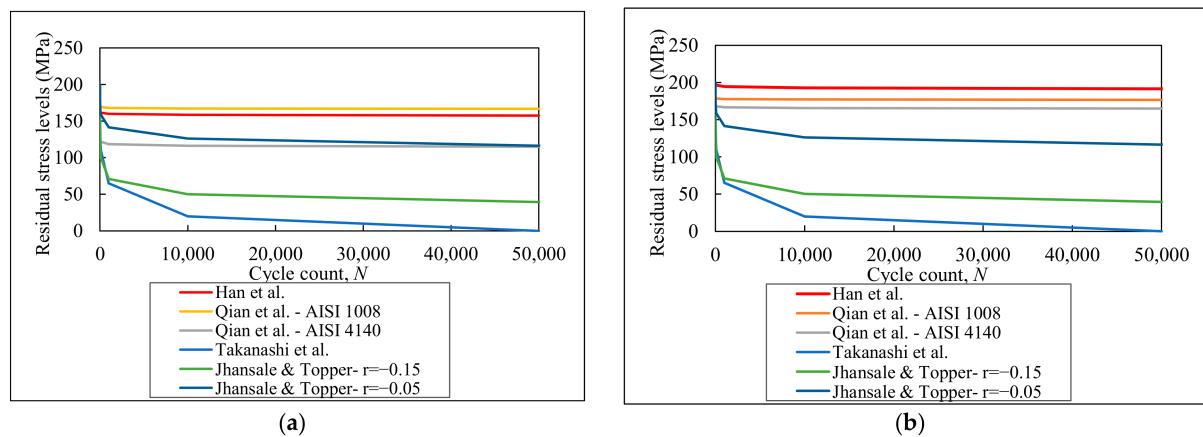


Figure 11. Comparison in predicted residual stress relaxation, with the empirical relationships given in [50–53] for a single element with initial residual stress of 200 MPa, subjected to a cyclic stress range of (a) 300 MPa, and (b) 200 MPa.

4. Prediction of Weld Residual Stress Effects on Fatigue Life

4.1. Introduction of Weld-Induced Residual Stresses into the Modelling Framework

The previous parts of the present study have primarily focused on understanding the detrimental impact of tensile residual stresses on fatigue performance. However, as discussed in [54], experimental evidence suggests that significant levels of welding-induced residual stresses were inherently present in the large-scale dogbone welded specimens tested in the SLIC programme. From a practical standpoint, two assumptions can be made when assessing the influence of residual stresses on the fatigue life of monopiles using the SLIC data. The first assumption is that the SLIC mean S-N curve already accounted for the fatigue behaviour associated with the weld-induced residual stress field predicted by the FEA simulation. The second assumption is that the levels of residual stresses present within the SLIC specimens are reduced in comparison to the initial (predicted) residual stress field in welded plates, due to the cutting procedure used for sample preparation, and continuous cyclic loading.

For clarity, three reference states are considered in the following analysis. The baseline case is calibrated directly to the SLIC as-welded mean S-N curve without explicitly introducing a residual stress field; therefore, it implicitly includes the combined influence of the residual stresses and relaxation present within the tested specimens. The added residual stress case introduces the numerically predicted weld residual stress field into this baseline model and is used as a sensitivity assessment of a higher or more fully retained residual stress state. Finally, the residual stress-free case is obtained through back-calibration, whereby the model parameters are first adjusted to reproduce the SLIC mean curve with the numerical residual stresses present, before removing the numeric residual stress field while retaining the recalibrated parameters. This final case represents a hypothetical comparative estimate and not a directly validated prediction of the actual residual stress-free SLIC response.

To assess the damaging effects of residual stresses present in large-scale as-welded structures, the residual stress profile obtained from the welding simulation of the 50 mm thick S355 plate is superimposed in the fatigue analysis to evaluate the reduction in fatigue life associated with higher levels of tensile residual stresses. As shown in Figure 12, this procedure was carried out for three representative modelling approaches. The first corresponds to the full addition of a residual stress profile predicted from weld simulation on 50 mm thick S355 plates. The second case represents a stress-independent relaxation formulation, for which the Takanashi model was adopted, leading to a uniform reduction

in residual stresses irrespective of local stress amplitude. The third case employs the stress-dependent relaxation model proposed by Qian et al., which explicitly accounts for the dependence of residual stress relaxation on the maximum combined stress level and therefore captures more realistic redistribution behaviour under varying fatigue loading conditions. Whilst the model is calibrated to the SLIC mean dataset, which is assumed to be inclusive of residual stresses, the results can be isolated in a comparative sense, and highlight how the inclusion of additional residual stresses reduce fatigue life at different rates depending on the relaxation model employed in the analysis.

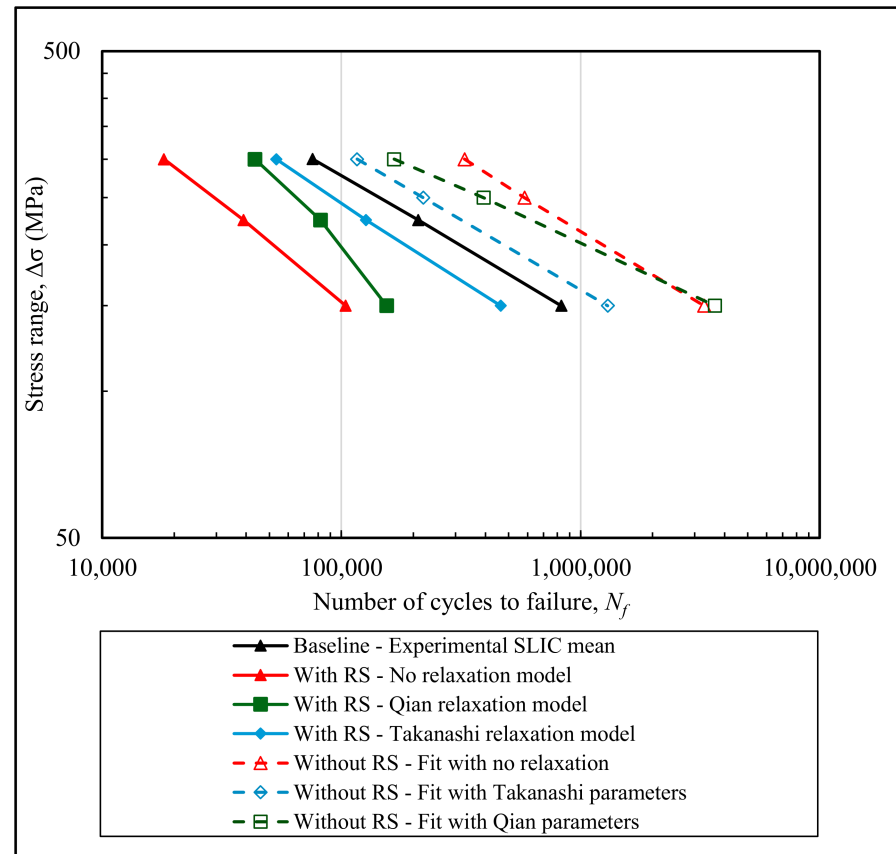


Figure 12. Predicted change in SLIC mean curve with residual stresses added or removed prior to fatigue loading, and assuming various residual stress relaxation models.

Furthermore, based on the first assumption, it is reasonable to anticipate that the SLIC as-welded mean curve would shift towards the right, corresponding to an increase in the number of cycles to failure, if the damaging residual stresses predicted from weld simulation of 50 mm thick S355 plate and assumed to be present in the SLIC specimens could be fully removed prior to fatigue loading. In reality, complete removal of weld residual stresses, particularly in large-scale offshore wind turbine monopile foundations, is unlikely; however, this conceptual exercise provides a useful framework for bounding the influence of residual stresses and assessing their contribution to fatigue damage accumulation.

In order to quantify this potential shift due to the removal of welding residual stresses, the simulation framework was re-executed through a back-calibration procedure. In this procedure, the calibration parameters, specifically α_T and the exponent n , were modified so that the simulation reproduced the SLIC mean curve when the numerically predicted residual stresses were included, with the relevant relaxation technique applied. The recalibrated parameter sets required to reproduce agreement with the SLIC as-welded mean curve are summarised in Table 4. The procedure utilised in prediction of the residual stress-free curve can be summarised as follows:

- (1) The numerically predicted weld residual stress field was introduced into the phase-field fatigue model, with the relevant relaxation model (if utilised) applied.
- (2) The calibration parameters α_T and n were adjusted until the resulting S-N curve aligned with the experimentally measured SLIC as-welded mean curve.
- (3) The residual stress field was then removed, while keeping the recalibrated parameters unchanged, to predict the hypothetical S-N response in the absence of welding residual stresses.

Table 4. Updated calibration parameters to match resulting curves with SLIC as-welded mean.

Model	α_T	n
Baseline	475	1.10
No relaxation	2046	1.10
Takanashi (stress-independent relaxation)	726	1.10
Qian (stress-dependent relaxation)	90	1.77

In a similar manner to before, the procedure was carried out for three representative models, the first being in the absence of any relaxation, the second a stress-independent model with residual stresses being based solely on cycle number, and the third a stress-dependent model based on cycle number and local stress magnitude. It can be seen in Figure 12 that the stress-dependent relaxation formulation requires an adjustment of the exponent n , which reflects a modification in the inverse slope of the S-N response associated with various extents of residual stress relaxation at different levels of stress range.

Following this calibration stage, the recalibrated parameters were subsequently used to estimate the hypothetical position of the SLIC mean S-N curve in the absence of all residual stresses by removing the numerical residual stress field while keeping the updated calibration parameters unchanged. This provides a comparative baseline against which the influence of weld-induced residual stresses can be isolated. The resulting shift in the fatigue curve, which is illustrated in Figure 12, indicates a systematic increase in predicted fatigue life when damaging residual stresses are removed from the weld. This shift is particularly pronounced in the high-cycle fatigue regime, where residual stresses apply a stronger influence on crack initiation. Overall, the results presented in Figure 12 provide a more comprehensive understanding of how residual stress modelling assumptions directly influence fatigue life predictions and highlight the importance of accurate residual stress representation in large-scale welded offshore wind monopile structures.

4.2. Effectiveness of Replicating Residual Stress Effects with Constant R-Ratios

As previously discussed, existing fatigue design and life assessment standards and guidelines adopt different approaches when accounting for the influence of welding residual stresses on fatigue performance. In S-N curve-based guidelines and recommended practice, such as DNV-RP-C203, the effects of welding residual stresses are generally assumed to be implicitly incorporated within the design curves. Consequently, no additional correction for mean stress or residual stress is typically required, as the design curves are intended to account for the worst-case scenario in the fatigue behaviour of welded joints. This philosophy has proven effective for conventional fatigue design, where the primary objective is to provide a robust and conservative design methodology based on extensive experimental evidence rather than an explicit representation of the underlying residual stress field. In contrast, structural integrity and flaw assessment procedures, such as BS 7910, adopt a slightly different approach by explicitly accounting for the presence of tensile welding residual stresses. BS 7910 recommends the application of a high effective

stress ratio, typically $R \geq 0.5$, when assessing fatigue crack growth in welded structures. This approach reflects the observation that high tensile residual stresses effectively increase the mean stress experienced during cyclic loading, hence accelerating fatigue damage.

The equivalent R -ratio approach is most applicable when a representative weld residual stress field is unavailable or cannot be explicitly incorporated within the analysis. This may include fatigue testing of small or machined specimens in which residual stresses have been partially released, or simplified engineering assessments where detailed weld simulations and residual stress measurements are unavailable. In such cases, a positive R -ratio provides a practical means of approximating or conservatively bounding the mean stress effect associated with tensile welding residual stresses. However, it remains a simplified representation of the complex, spatially varying stress state present in welded components.

Based on the information provided above, comparing the explicitly modelled residual stress results with predictions obtained using equivalent R -ratios provides a useful benchmark to evaluate the effectiveness of the high- R -ratio approach in fatigue life assessment of welded structures. However, it should be noted that representing weld residual stresses through a single global R -ratio is clearly a simplified approach. In reality, welding residual stresses are highly non-uniform, self-equilibrating, and vary significantly throughout the thickness and across the weld geometry. Furthermore, their magnitude redistributes throughout the service life of the structure due to cyclic loading and local plasticity. These characteristics cannot be adequately represented by a constant mean stress correction or a single effective R -ratio applied uniformly across the entire structure. Consequently, the explicit incorporation of a three-dimensional residual stress field within the phase-field fatigue model provides a more physically representative description of the stress state governing fatigue behaviour in welded structures.

To evaluate the effectiveness of the conventional R -ratio approach, additional simulations were performed using the recalibrated material parameters presented in Table 4. Two representative stress ratios of $R = 0.25$ and $R = 0.5$ were considered, corresponding to values commonly adopted in fatigue assessment procedures to represent moderate and high tensile residual stress conditions. These predictions were then compared directly with the fatigue lives obtained from the explicit residual stress modelling approach. The comparison enables the degree to which an equivalent R -ratio can reproduce the influence of a realistic weld-induced residual stress field to be quantified, while also highlighting the limitations of simplified mean stress corrections. The resulting fatigue S-N curves are presented in Figure 13 for the no residual stress relaxation condition. Knowing that the SLIC tests were carried out with the $R = 0.1$, the results in Figure 13a demonstrate that, if the weld residual stresses predicted from the weld simulations were removed, the fatigue life under the same loading conditions would increase substantially. This highlights the significant detrimental influence of tensile welding residual stresses on the fatigue performance of welded joints. Figure 13a also illustrates that starting from the back-engineered SLIC mean curve corresponding to a residual stress-free condition at $R = 0.1$ as a reference curve, progressively increasing the stress ratio results in a continuous reduction in fatigue life, which is consistent with the well-established influence of increasing mean stress on fatigue behaviour. The experimental SLIC mean curve is observed to lie between the fatigue curves extrapolated from the residual stress-free reference using equivalent stress ratios of $R = 0.25$ and $R = 0.5$. This suggests that an equivalent R -ratio approach can provide a reasonable approximation of the overall influence of welding residual stresses. Furthermore, the results shown in Figure 13b show that when residual stress relaxation is considered, a lower R -ratio of approximately $R = 0.25$ provides more agreement with the SLIC as-welded mean curve, indicating that higher R -ratios may be accurate prior to relaxation.

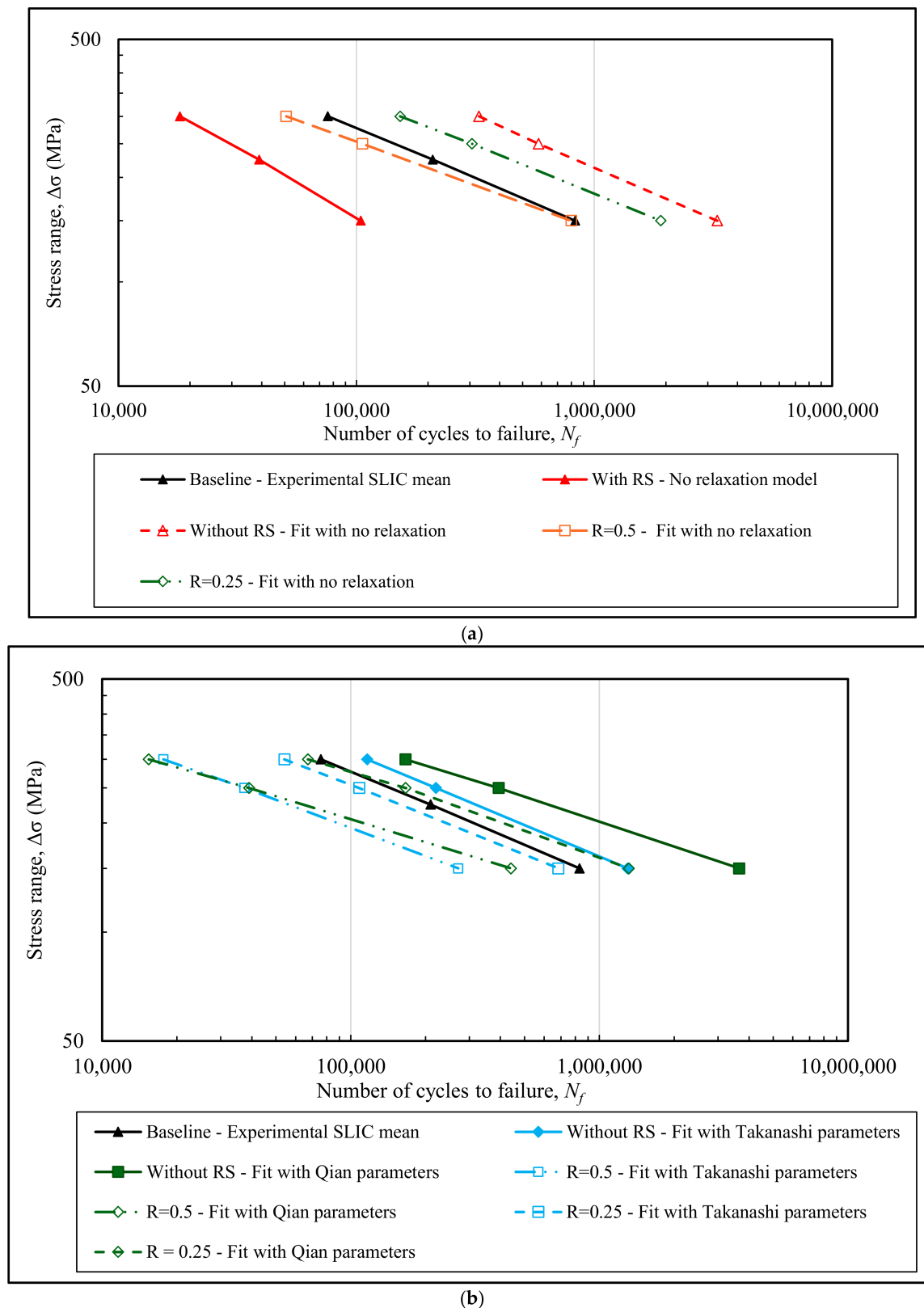


Figure 13. Effectiveness of representing residual stresses through imposed R-ratios assuming (a) no residual stress relaxation and (b) relaxation built upon empirical relationships.

However, Figure 13 also demonstrates the limitations of this simplified methodology. Although adopting a higher stress ratio of $R = 0.5$ provides a more conservative estimate of the experimental fatigue behaviour than $R = 0.25$, the constant R-ratio approach is unable to reproduce the change in the inverse slope of the S-N curve observed when weld residual

stresses are explicitly modelled. This is because a single global R -ratio cannot capture the spatially varying, self-equilibrating nature of the residual stress field or its evolution through residual stress relaxation during cyclic loading. Consequently, explicit modelling of the weld residual stress field offers a more physically representative prediction of fatigue behaviour, particularly when the objective is to quantify the influence of residual stress redistribution throughout the service life of welded offshore wind monopile structures.

5. Discussion

Phase-field models provide a powerful framework for investigating the influence of complex physical phenomena on fatigue crack initiation and propagation. Through appropriate physics-based extensions, these models can accurately represent the accumulation of fatigue damage while retaining a strong mechanistic basis. Once calibrated against experimental data, they are capable of reproducing experimentally derived S-N fatigue curves, hence providing a reliable baseline from which additional physical mechanisms can be incorporated. A key advantage of this numerical approach is its repeatability; unlike experimental fatigue testing where significant scatter is unavoidable, simulations allow individual variables to be modified in a controlled manner. This enables the contribution of specific factors to fatigue life to be isolated and quantified without the uncertainty associated with experimental variability, which would facilitate fundamental understanding of the mechanisms governing fatigue behaviour. In the present study, the existing phase-field fatigue formulation was extended to explicitly incorporate weld-induced residual stresses, allowing their direct contribution to fatigue damage evolution to be investigated. Initial simulations employing uniform residual stress fields demonstrated good agreement with the expected reduction in fatigue life predicted using an equivalent positive R -ratio derived from a Walker-type mean stress correction. Although the sensitivity of fatigue life to mean stress is inherently material-dependent, the close agreement between the analytical R -ratio predictions and the uniform residual stress implementation provides confidence that the proposed formulation accurately captures the influence of residual stresses through their modification of the effective mean stress. Furthermore, this agreement was maintained over a broad range of positive R -ratios, including both moderate and high values, indicating that the model is capable of representing a wide spectrum of residual stress magnitudes.

Modelling the residual stress field generated during welding presented a greater challenge. The predicted stress field was found to be sensitive to a number of modelling assumptions, including the temperature-dependent material properties, peak weld temperature, inter-pass temperature, cooling time, ambient conditions, and applied boundary conditions. In addition, the available experimental residual stress measurements for offshore wind monopile-related welds are limited, as contour method measurements typically provide residual stresses in only a single direction. This makes full validation of the complete residual stress field difficult. Differences in specimen geometry, weld sequencing, material properties, and measurement procedures can also alter both the magnitude and distribution of the residual stresses. Additionally, the contour method itself may also introduce uncertainty through the cutting process and through the material properties assumed during the reconstruction of the stress field. Despite these limitations, the simulated longitudinal residual stress field showed good agreement with the available reference results. In particular, the model reproduced the key features observed experimentally, including high tensile residual stresses within the centre of each V-groove, a lower stress region between the two tensile peaks, and maximum values approaching the yield strength of the material. While precise tuning of the residual stress field is difficult due to the number of uncertain input parameters, the agreement in the longitudinal direction provides confidence that the predicted stress field is representative of the general residual stress state expected in

a large-scale multi-pass double V-groove butt weld. On this basis, the predicted residual stress components in the remaining directions were considered suitable for use within the phase-field fatigue model. However, some uncertainty remains, particularly for the transverse residual stress component, which acts parallel to the applied fatigue loading direction and therefore directly influences the predicted fatigue response.

This predicted residual stress field is particularly damaging, not only due to the magnitude of the peak residual stresses but also due to the location of higher tensile residual stresses being within the centre of the weld, and at the weld toe. This location is a cause for concern as fatigue cracks predominantly initiate at the weld toe, due to the geometry change creating a stress concentration factor increasing the local stresses, and the vulnerable microstructure within this region from the increased temperatures in the welding process. The superposition of these effects can significantly increase the local driving force for fatigue damage, even when the nominal applied stress range is relatively low. The impact of these residual stresses on fatigue life can be noticed in the predicted reduction in fatigue resistance shown in the S-N curve (Figure 12), with the number of cycles to failure dropping dramatically when the full residual stress profile is applied. The reduction was most pronounced at lower applied stress ranges, where the residual stress contribution represents a larger proportion of the total local stress state. For example, at an applied stress range of 150 MPa, the predicted number of cycles to failure reduced from $N_f = 753,000$ cycles in the baseline case to $N_f = 104,282$ when the full residual stress field was applied. This corresponds to a reduction in fatigue life of approximately a factor of eight. The results therefore suggest that tensile welding residual stresses can have a substantial influence on fatigue life, particularly in the high-cycle fatigue region.

However, it should be noted that the direct interpretation of this result requires caution. The baseline S-N curve was calibrated to the SLIC as-welded test data, meaning that the experimental curve already contains the influence of welding residual stresses and any relaxation that occurred during testing. Therefore, the baseline condition should not be regarded as a true residual stress-free state. Instead, the comparison demonstrates the relative sensitivity of the phase-field model to an explicitly imposed residual stress field. The implication is that, in the absence of tensile welding residual stresses, the underlying fatigue resistance of the welded geometry may be significantly greater than that represented by the SLIC as-welded mean S-N curve. The inclusion of residual stress relaxation is therefore important when assessing the realistic fatigue response of welded components. While the non-relaxed residual stress simulations provide a conservative estimate, they do not account for the redistribution of residual stresses that can occur when the combined applied and residual stress exceeds the local yield strength. This is particularly relevant for welded structures, where tensile residual stresses may already be close to yield before external loading is applied. Even a moderate applied cyclic load may therefore produce local yielding and residual stress relaxation near the weld toe. This relaxation can reduce the damaging mean stress contribution and may partially recover fatigue life relative to the non-relaxed case.

The empirical residual stress relaxation models considered in this study all reproduced the commonly observed trend that residual stress relaxation is front-loaded, with the largest reduction occurring during the early cycles. However, the magnitude of relaxation differed between models, particularly due to differences in whether the relaxation law depends only on cycle count or also on the combined stress state. At higher applied cyclic stresses, the differences between the relaxation models were less significant. This is because the combined applied and residual stresses near the weld toe were sufficiently high that all models predicted substantial relaxation in the most damaging region. As a result, the variation in predicted fatigue lives was relatively small at the higher stress ranges. At

lower applied stresses, however, the stress-dependent models showed greater sensitivity to the magnitude and distribution of residual stress. In these cases, only regions where the combined stress exceeded the relevant relaxation threshold experienced significant relaxation. This resulted in a steeper predicted S-N response for the Qian and Han models compared with the cycle-count-based relaxation models, with the stress-dependent models predicting that residual stresses prove more impactful and harmful at low stress ranges, providing a similar drop in fatigue life to the model without relaxation, by an approximate factor of 6.

The use of empirical relaxation models provides a computationally efficient method for considering residual stress relaxation within a linear elastic fatigue model. This is beneficial for high-cycle fatigue simulations, where a fully elastic–plastic cyclic analysis would be computationally expensive. Nevertheless, this approach introduces limitations. The relaxation models are material-specific and were originally calibrated using steels and specimen types that do not exactly match the present S355 welded plate. In addition, stress-dependent relaxation models may produce non-uniform reductions in the residual stress field, which can disturb the self-equilibrating nature of the original residual stress distribution. Therefore, while the empirical models provide useful insight into the likely influence of residual stress relaxation, the predicted fatigue lives should be interpreted as comparative rather than definitive.

Overall, the results show that residual stress relaxation can substantially reduce the damaging effect of welding-induced tensile residual stresses. All relaxation models predicted more feasible fatigue lives than the non-relaxed residual stress case, indicating that assuming a constant residual stress field throughout fatigue loading is likely to be overly conservative. However, the extent of this recovery remains uncertain and depends strongly on the relaxation model selected, the calibration parameters used, and the assumed residual stress distribution. The cycle-based relaxation method produced a smaller reduction in fatigue resistance, typically around 30–45% across the stress ranges considered, whereas the stress-dependent Qian model produced a stronger reduction, ranging from approximately 40% at higher stress ranges to more than 80% at the lower stress range. Further experimental work linking measured residual stress relaxation directly to fatigue life in large-scale welded S355 specimens would be required to validate the most appropriate relaxation approach.

The curves were produced by recalibrating the model, ensuring that, with residual stresses (and relevant relaxation models) applied, the simulated output would align with the measured mean curve of the SLIC as-welded data points. Running the model with the updated calibration parameters, and removing the residual stresses utilised within the re-calibration procedure, produced the predicted mean curve for the SLIC as-welded samples with all residual stresses removed. This gives useful insight into the possible role of residual stresses in reducing fatigue resistance. The model without any residual stress relaxation predicted the largest shift at 150 MPa; this corresponded to an increase from 7.53×10^5 cycles for the SLIC mean to approximately 3.29×10^6 cycles when residual stresses were removed, giving an increase of roughly four times. However, this result is likely to be overly optimistic, as it assumes that the full residual stress field remains active throughout fatigue loading.

The process of recalibrating with residual stresses was repeated, taking residual stress relaxation into account through a stress-independent model (Takanashi) and a stress-dependent model (Qian). The results with the stress-independent relaxation model (Takanashi) show a modest increase in fatigue lives (approximately 1.5 times higher), when residual stresses are removed in their entirety. This increase is limited as the relaxation is solely dependent on the number of cycles, and features a front-heavy relaxation process,

significantly reducing the applied residual stresses for the majority of the test duration. The predicted fatigue curve with residual stresses removed, derived from the calibration constants using the Qian residual stress formulation, shows a change in inverse slope from the measured mean curve, with a lower increase in fatigue resistance at higher stresses relative to the significant increase in fatigue resistance at low stresses. This is due to the model being dependent on the ratio of total stress to yield stress, predicting a higher level of residual stress relaxation occurring at higher stress ranges, and low relaxation at lower stress ranges. This inverse slope transitions the curve from the generous relaxation model to the non-relaxed model at lower stress ranges.

In fatigue testing of welded samples, when the residual stress field is unknown or assumed to have been released during sample preparation, it is common to apply a high R -ratio, typically $R \geq 0.5$, to represent the effect of tensile residual stresses. To gauge the effectiveness of this approach, the predicted residual stress-free curves were simulated with applied R -ratios of $R = 0.25$ and $R = 0.5$. The results indicate that applying a positive R -ratio can help shift the residual stress-free response back towards the fatigue life expected for as-welded samples containing residual stresses. However, the accuracy of this approach depends strongly on the R -ratio selected and on the assumed level of residual stress relaxation. For the model without residual stress relaxation, an imposed R -ratio of $R = 0.5$ produced a response close to the SLIC mean curve, although a slight change in inverse slope remained. This change in the slope is likely due to the significant maximum stresses required to produce high R -ratios, combined with the stress concentrator at the weld toe putting that area within the material into the low-cycle fatigue region. However, when residual stress relaxation was included, the models aligned more closely with the SLIC mean using a lower, but still positive, R -ratio of ($R = 0.25$). This suggests that using a single high R -ratio may be conservative. Nevertheless, a global R -ratio remains a simplified representation, as it cannot fully capture the spatial distribution, self-equilibrating nature, or progressive relaxation of welding residual stresses.

6. Limitations and Future Work

Although the modelling framework provides useful insight into the influence of welding residual stresses and their relaxation on fatigue life, several limitations remain. The predicted longitudinal residual stress field showed good agreement with available experimental measurements; however, comparable data were unavailable for the transverse and normal components. These therefore remain numerical predictions with the transverse component being particularly important as it acts parallel to the applied fatigue loading direction. Future experimental investigations should seek to characterise the complete multiaxial residual stress field within large-scale S355 weldments, allowing the predicted stress components and their influence on fatigue life to be more comprehensively validated.

Furthermore, the unknown nature of residual stress relaxation remains. The presented empirical relationships may provide insight into how the predicted levels of relaxation effect the fatigue resistance; however, the relationship is calibrated for steels of differing grades, making a direct prediction unfeasible. A better understanding of the material-specific residual stress procedure would provide a more robust simulation procedure, giving a better view of the residual stresses present at the end of the structure's lifecycle. The unknowns priorly discussed highlight the opportunity for experimental-based research to fill a gap within the existing research, and allow for numerical modelling to be further assessed and implemented.

7. Conclusions

This study developed and applied a phase-field fatigue modelling framework to investigate the influence of welding-induced residual stresses on the fatigue life of large-scale welded S355 steel plates. The influence of residual stress on subsequent fatigue life was evaluated in the presence and absence of stress relaxation models. The key findings of this study are as follows:

- The thermal–mechanical welding model reproduced the principal features of the measured longitudinal residual stress field, including tensile peaks approaching the yield strength within the weld region and at the natural crack initiation site of the weld toe.
- In the absence of residual stress relaxation, the predicted tensile residual stresses from the welding procedure have a devastating effect on the fatigue life, dropping it by a factor of 4–8 dependent on the applied stress range.
- When residual stress relaxation was introduced, the fatigue life only fell by less than a factor of 2 at high stress ranges; however, when using a stress-dependent relaxation model, a change in the fatigue curve inverse slope was recorded resulting in a drop in fatigue life more akin to that of the model without relaxation.
- The simulation process can be extended to back calibrate the location of the mean curve in the absence of residual stresses, and the increase in fatigue life with residual stresses removed can be up to times four higher than the measured data.
- The common approach of accounting for residual stresses by introducing a higher mean stress provided a practical approximation of residual stress effects, with an R -ratio of $R = 0.25$ showing good correlation when residual stress relaxation is included; however, $R = 0.5$ provides a more conservative estimation that aligns well with the assumption that no residual stress relaxation is present.

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Appendix A. Temperature-Dependent Properties

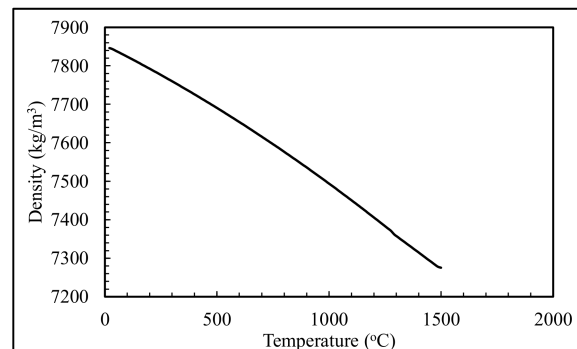


Figure A1. Temperature-dependent density.

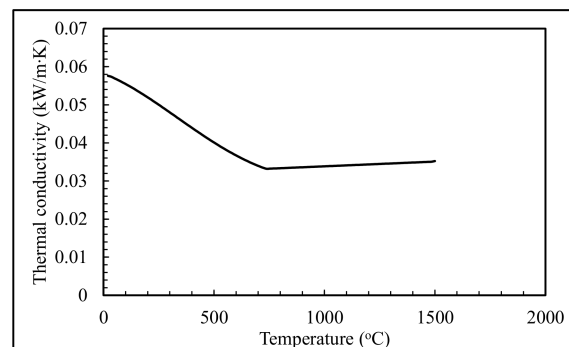


Figure A2. Temperature-dependent thermal conductivity.

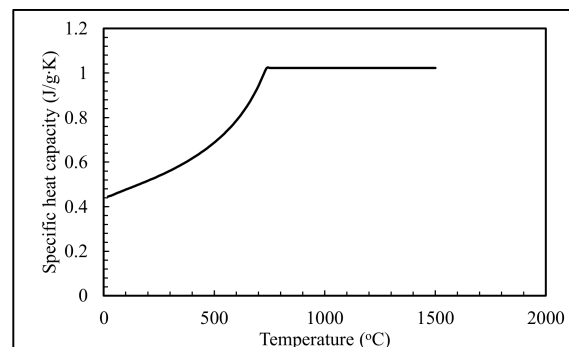


Figure A3. Temperature-dependent specific heat capacity.

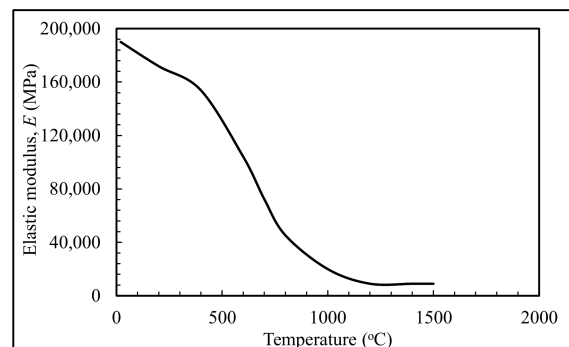


Figure A4. Temperature-dependent Young's Modulus.

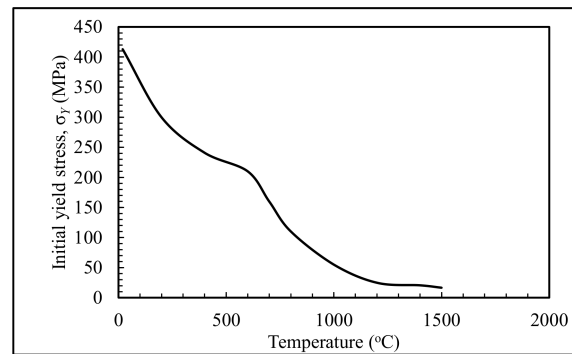


Figure A5. Temperature-dependent initial yield stress.

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