

Material pre-straining effects on fracture toughness variation in offshore wind turbine foundations

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ARTICLE INFO

Keywords:

Fracture toughness
S355
Offshore wind
Material pre-straining
Manufacturing effects

ABSTRACT

S355 structural steel is a commonly used material in the fabrication of foundation structures of offshore wind turbines, which are predominantly supported using monopiles. During the manufacturing process of monopile foundations, S355 steel plates are pre-strained via a three point bending and rolling process, which subsequently changes the mechanical, fatigue and fracture properties of the material. The aim of this study is to investigate the variation in fracture toughness of S355 material by considering a range of pre-strain levels induced during the manufacturing process. Fracture toughness tests have been performed on compact tension specimens made of the as-received, 5% and 10% pre-strained S355 material. The test results have shown that the fracture toughness of the material decreases as the percentage of pre-straining increases. An empirical correlation has been derived between the yield strength of the material, the plastic pre-strain level and the fracture toughness values. The drawn relationship can potentially be utilised in the life assessment of offshore wind turbine monopile foundations to give a relatively accurate estimate of the remaining life by considering realistic values of fracture toughness post-fabrication, which results in better informed design and assessment.

1. Introduction

Offshore wind is a rapidly maturing industry and is currently one of the leading sources of renewable energy for electricity production, with the EU aiming to reach 100GW of offshore wind capacity by 2030 [1,2]. The UK has made an ambitious plan to generate 40GW of electricity from offshore wind energy by 2030 [3]. This push for an increase in installed offshore wind capacity has been driven by the evidence of [4] climate change, thus not only the UK government but also many other governments have decided to look into renewable sources for reducing CO₂ emissions [2]. As a result of this, the offshore wind industry is continuously developing wind farms of higher capacities and looking to install them in deeper waters [5,6].

Foundation support structures and their design requirements are an important aspect to be considered in the future development of offshore wind projects [7,8]. There are many different types of support structures to be considered for offshore wind structures, the most common types being gravity based structures (GBS), monopiles, tripods, jackets and floating structures [9]. Monopile structures are single cylindrical tube structures that are fixed into the seabed, and thus are the simplest technical solution for shallow waters of up to 25 m. Tripods are three legged support structures connected to the frame of a central pipe that sits on top of a tripod frame structure.

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Nomenclature

a	Crack length
a_0	Initial crack length before pre-cracking
a_i	Crack length after pre-cracking
a_f	Final crack length after fracture toughness testing
Δa	Increment of crack growth
Δa_{max}	Maximum allowable crack extension in fracture toughness tests
B	Specimen thickness
B_e	Effective thickness
B_n	Specimen net thickness between side grooves
b_0	Uncracked ligament, $(W-a_0)$
C_0	Unloading compliance
E	Young's Modulus
E_M	Effective Young's modulus in fracture toughness data analysis
J_{IC}	Critical value of J for fracture under Mode I loading conditions
W	Specimen width
η	Factor relating J to load and displacement measurements
$\sigma_{0.2}$	0.2% proof stress
σ_{UTS}	Ultimate tensile stress
σ_y	Yield stress
ϵ_p	Plastic strain

Tripods can use either piles or suction bucket anchors. Due to its wide base, this structure has a large resistance against overturning. Jacket structures are very similar to tripod structures and consist of three or four legs connected to each other via slender braces, and then are piled into the seabed. This system has been adapted from oil and gas concepts, which have been previously used for decades. Floating structures are still in the early stages of development and are set to be utilised in deeper waters. There are numerous floating structure concepts which are being developed and adapted from the oil and gas industry, which are starting to yield good results [10–13].

Most offshore wind developers have selected the monopiles in the last decade due to its simple design and production, and thus low unit cost [10,14]. Monopile support structures are installed into the seabed and during their service life they are subjected to corrosion-fatigue damage due to the constant exertion of wave, wind and current forces in the corrosive environments [4,15–20]. An important issue to be considered in the structural integrity assessment of offshore wind monopile structures is the influence of material pre-straining, introduced into the structures during fabrication processes, on the mechanical response and fracture behaviour of the material. The manufacture of monopiles is conducted in three phases: rolling, bending and welding. S355 plates are firstly hot rolled, before being cut to size [21]. These plates are then cold rolled into cylindrical cans via the three roll bending process, which involves a roller being applied to induce bending of the steel, and then longitudinally welded [22]. These individual cans are then circumferentially welded together until the desired monopile length is achieved [22]. This process is a continuous manufacturing process, and has a low set-up cost [22]. As a result of the monopile fabrication process, a range of plastic strain levels can be found across the wall thickness, ranging from compressive to tensile [22].

From previous research studies it has been understood that varying levels of plastic pre-strain can affect the mechanical and fracture properties of steel. For many metallic materials it has been observed that material pre-straining results in a reduction in the fracture toughness properties of the material [23]. The effects of material pre-straining is a topic of research within the pipelines industry, as pipelines can be permanently plastically deformed via force (for example indentations), or due to subsidence, frost heave or earthquake/seismic loading during service [24,25]. In a study conducted by Sivaprasad *et al.* [26], HSLA-80 and HSLA-100 steels were pre-tensioned and fracture toughness testing was conducted. From the results it was concluded that HSLA steels did not show any significant changes in fracture toughness up to a plastic strain of 2%; however, pre-straining over 2% presented a deterioration in fracture toughness for HSLA steels [27]. The effects of plastic pre-compression on the fracture toughness of 316H steels was investigated by Mehmanparast *et al.* [28–30]. In their study 316H stainless steel was pre-compressed to 8% plastic strain and fracture toughness tests were performed. From the results it was concluded that there was a decrease in fracture toughness values in the pre-compressed material compared to the as-received material state. Liaw and Landes reported similar results for both 316 and 4340 steels where both materials were monotonically pre-tensioned to 2% and 5% and fracture toughness tests were performed [31].

Previous works from other researchers suggest that pre-straining the metals results in a change in the mobile dislocation density and this can control the fracture toughness in metallic materials [32–38]. As metals are pre-strained, the dislocation structure and distribution is altered [36]. Dislocation density generally increases with pre-straining, and this can result in a higher yield strength and lower ductility [37]. In some instances, dislocation density can decrease at low pre-straining levels, due to the destruction of localised dislocation. With increasing pre-straining, new dislocations start forming which therefore increases the dislocation density [38]; however, the formation of small voids may induce fracture and therefore pre-strained material exhibits a lower fracture resistance [29].

The same effect will be present at varying levels of plastic-pre-strains across the thickness of monopile structures; however, there is very little information on the fracture behaviour of S355 steel with relation to material pre-strain. In order to gain an insight into the implications of material pre-straining on monopile structures, an experimental programme has been devised. As the conservative scenario is being investigated, pre-strain levels that have been investigated are only from the tensile regions for this particular project, assuming that tensile pre-strain results in similar or more detrimental effects on the fracture toughness compared to compressive pre-strains. Therefore, in this work the effect of material tensile pre-straining levels of up to 10% on fracture properties of S355 structural steel has been investigated.

2. Material pre-conditioning and specimen preparation

The material used in this work is S355G10+M structural steel, which is widely used in the manufacture of offshore wind monopile structures. The chemical composition of the material used in this study is shown in Table 1. All specimens examined in this study were extracted from the mid-thickness of a 90 mm thick S355G10+M plate.

To investigate the effects of material pre-straining on mechanical properties and fracture behaviour of the material, three material states were considered: (1) As-received (also known as the base metal with 0% plastic strain), (2) uniformly pre-tensioned material to 5% plastic strain, and (3) uniformly pre-tensioned material to 10% plastic strain. These pre-strain levels were chosen to investigate the effects of material pre-strain on fracture toughness of S355 over the examined stress range applied during the monopile fabrication process detailed in [22]. In addition, considering a wide range of pre-straining will result in a more conservative defect assessment in structural integrity procedures. In order to conduct experiments to determine the influence of material pre-strain, the material had to be uniformly plastically pre-strained first at room temperature, as shown in Fig. 1.

In order to quantify the tensile properties of the material, for each pre-strain level (i.e. 0%, 5% and 10%) two tensile tests were conducted on dog bone shape specimens extracted from the uniformly pre-strained material. The dog bone specimens for tensile tests had the thickness of 11 mm, width of 36 mm and gauge length of 48 mm. The “engineering stress vs. engineering strain” obtained from these tensile tests are graphically presented in Fig. 2 and the average values of mechanical properties obtained from two tests at each pre-strain level have been summarised in Table 2. From the table it can be seen that while the elastic Young’s modulus, E , has slightly increased by increasing the pre-strain level, there is an increase in the ultimate tensile strength, σ_{UTS} , and the yield stress of the pre-tensioned materials, σ_Y , as expected. In this study, the yield stress is defined as the most conservative value of yield in the non-linear (which is also discontinuous in the case of 0% pre-strain material) region of the tensile curves. Moreover, a 2% decrease in the axial strain at failure, ϵ_f , is also noticed between each pre-strain level. A similar trend can be observed with the axial strain at ultimate tensile strength, ϵ_{UTS} . In general, it can be seen in Table 2 and Fig. 2 that increasing the material pre-strain level increases the yield stress and decreases the failure strain of the material.

The tensile tests performed in this study were conducted in conjunction with digital image correlation (DIC) measurements to monitor the strain variations during the tests. DIC is an optical method of measuring displacements and strains on engineering materials subjected to different loading conditions. The technique involves spraying the specimens with speckle patterns which are used as reference points by the DIC software. The DIC software then tracks the movements of the speckle patterns and the output from the DIC will map the displacements of these speckles which can then be converted into strains. For this work, the software used was Istra 4D to view the strain maps and process the output data. DIC measurements on initial tests showed that pre-straining is consistent across the gauge region during the tensile tests before any necking occurs in the test specimen. Moreover, it was ensured that the specimens were lit adequately while DIC strain measurements were conducted. The contour plots of the axial strain distribution along the loading direction for 0%, 5% and 10% pre-strained specimens at the stress level of 500 MPa are shown in Figs. 3, 4 and 5, respectively. From the images it can be seen that the strain in the gauge region of the 0% material is much higher compared to the strain contours for the 5% and 10% material. This is due to the fact that the lower pre-strain material has a lower yield stress, which has resulted in larger plasticity hence greater strain values at 500 MPa in the as-received material compared to 5% and 10% pre-strained specimens. Also comparing the strain distribution maps in Figs. 4 and 5 it can be seen that at 500 MPa, the material with 10% pre-straining has exhibited lower strain levels compared to the 5% pre-strained material. This is due to the material hardening effect which has occurred by increasing the pre-strain level from 5% to 10%.

Upon characterisation of the mechanical properties of the material, for each of the pre-straining levels examined in this study, two large-scale dog bone specimens were prepared and pre-tensioned (see Fig. 1) to extract a sufficient number of fracture toughness test specimens. The large-scale dog bone samples for material pre-straining tests had the thickness of 16 mm, width of 90 mm and gauge length of 150 mm. The pre-straining of the large-scale dog bone samples was monitored using the DIC technique to interrupt the tests at 5% and 10% pre-strain levels. On completion of material pre-straining on the large-scale dog bone samples, compact tension, C(T), specimens were then fabricated from the as-received and pre-strained dog bones as shown in Fig. 6. For each pre-strain level, four C(T) specimens were extracted from two pre-strained large-scale dog bone samples (i.e. 2C(T)s from each large-scale dog bone sample),

Table 1
Composition wt% of S355G10+M.

C	Mn	Si	Cu	N	P	S	Cr	Ni	Mo	V
0.061	1.58	0.280	0.254	0.0041	0.0013	0.0007	0.034	0.342	0.06	0.001
Nb	As	Sn	Ti	Pb	B	Sb	Ca	Bi	Al-T	
0.022	0.003	0.001	0.003	0.000	0.0003	0.0010	0.0028	0.0001	0.032	

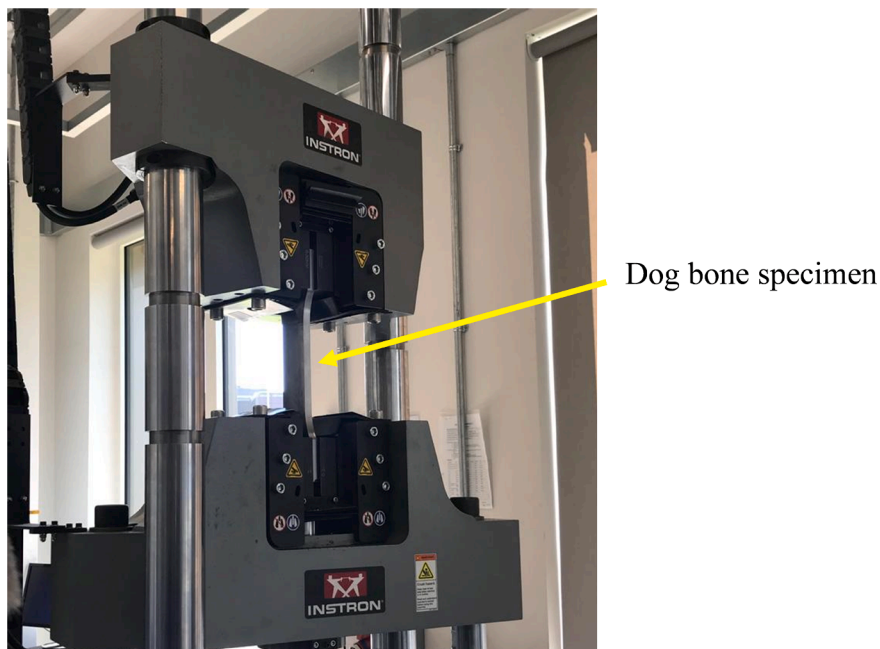


Fig. 1. Dog bone specimen undergoing pre-tensioning.

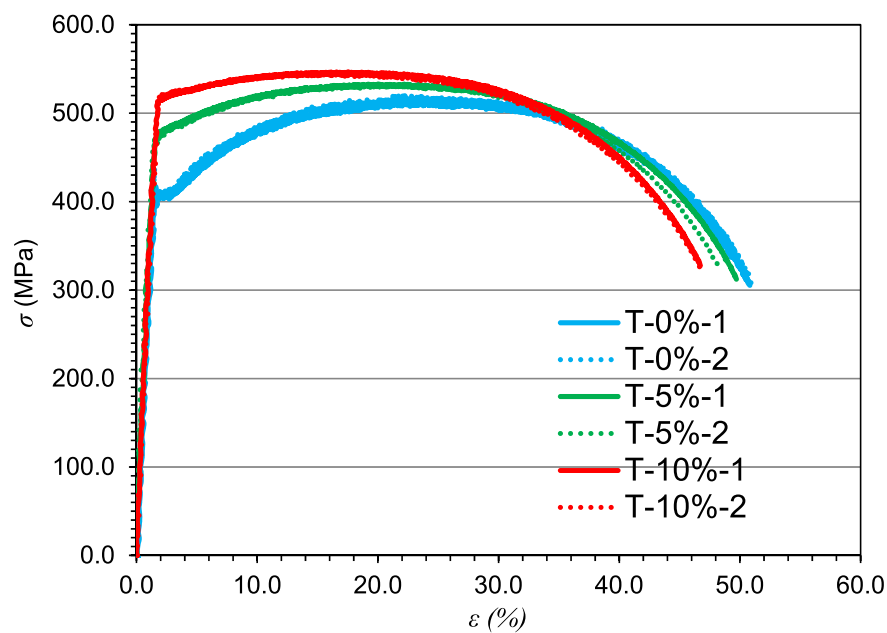


Fig. 2. Stress-Strain curves for 0%, 5% & 10% pre-strained materials.

Table 2

Average mechanical properties of as-received and pre-strained materials.

Tensile pre-strain level	E (GPa)	σ_Y (MPa)	σ_{UTS} (MPa)	ϵ_f (%)	ϵ_{UTS} (%)
0%	222	409	517	51	21
5%	255	468	535	49	18
10%	264	516	543	47	17

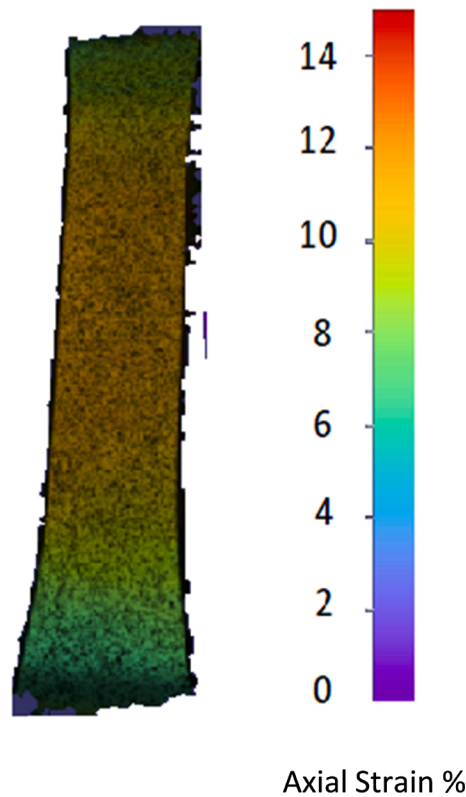


Fig. 3. Axial strain map of 0% pre-strained specimen at 500 MPa.

with the exception of 0% pre-strained material for which three samples were extracted and tested. The C(T) specimens were extracted with the loading axis parallel to the pre-straining direction. The specimen dimensions were chosen based on the guidelines proposed in the ESIS P2-92 standard; however, the thickness, B , was reduced to 15 mm to cater for the reduced load capacities during the large-scale dog bone pre-straining as shown in Table 3. From the table it can be seen that all the specimens had a width of $W = 50$ mm, total thickness of $B = 15$ mm, and net thickness between the side grooves of approximately $B_n = 10.5$ mm.

Moreover, the machined notch length introduced into the C(T) specimens, before fatigue pre-cracking, using the electrical discharge machining (EDM) technique was approximately $a_0 = 23$ mm. It is worth noting that all C(T) specimens were initially side-grooved by 30% of the total thickness and then were pre-fatigue cracked to promote a straight fronted crack growth during testing. The initial crack lengths after fatigue pre-cracking, a_i , are summarised in Table 3. As shown in Table 3, 11 samples were tested in total to examine the plastic pre-straining effects on the fracture toughness behaviour of the material. Each specimen has been denoted a unique sample ID which includes the specimen type, followed by the percentage of plastic-pre-strain and ending with the specimen number for each material state. For example, CT-0-1 indicates that the fracture toughness test was performed on the first C(T) specimen with 0% plastic pre-strain (as-received material state).

3. Fracture toughness testing and analysis

Fracture toughness tests can be conducted on C(T) specimens using the multiple specimen and single specimen approaches. The multiple specimen approach requires a number of nominally identical specimens to be loaded and unloaded after a specified amount of crack extension has occurred [32]. The single specimen approach involves multiple loading and unloading cycles on only one test specimen, where the crack extension would be estimated between each loading/unloading cycle using the unloading compliance data. There are a few standard test methods available for fracture toughness testing; however, the standards that are most commonly used to execute and analyse fracture toughness tests on metals are ASTM E1820, ESIS P2-92, ISO 12,135 and BS 7448-4 [33,34,39,40]. For this work the test method that has been followed is the procedure outlined in ESIS P2-92 [34] which enables direct comparison with the results available in the literature.

3.1. Testing methodology

Fracture toughness tests were performed on three as-received (CT-0-1, CT-0-2, CT-0-3), four 5% pre-strained (CT-5-1, CT-5-2, CT-5-3, CT-5-4) and four 10% pre-strained (CT-10-1, CT-10-2, CT-10-3, CT-10-4) specimens. Although it is recommended by standard

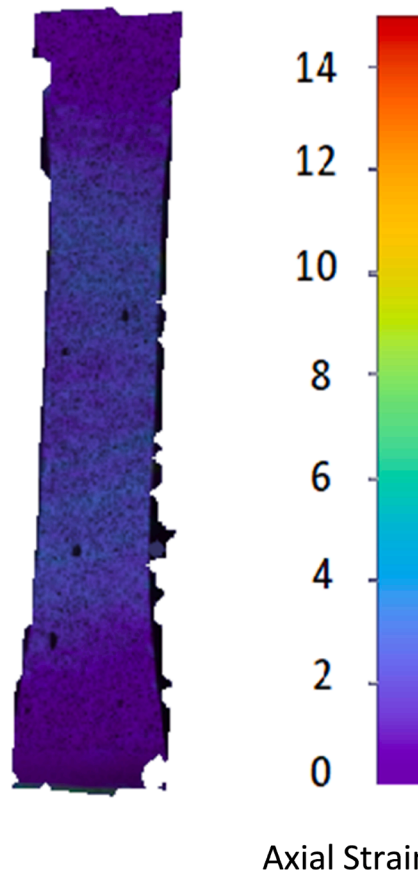


Fig. 4. Axial strain map of 5% pre-strained specimen at 500 MPa.

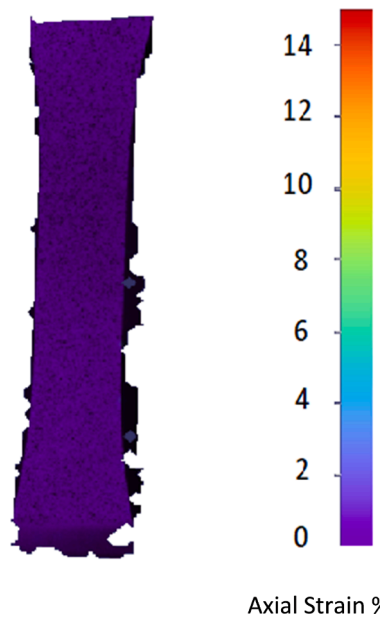


Fig. 5. Axial strain map of 10% pre-strained specimen at 500 MPa.

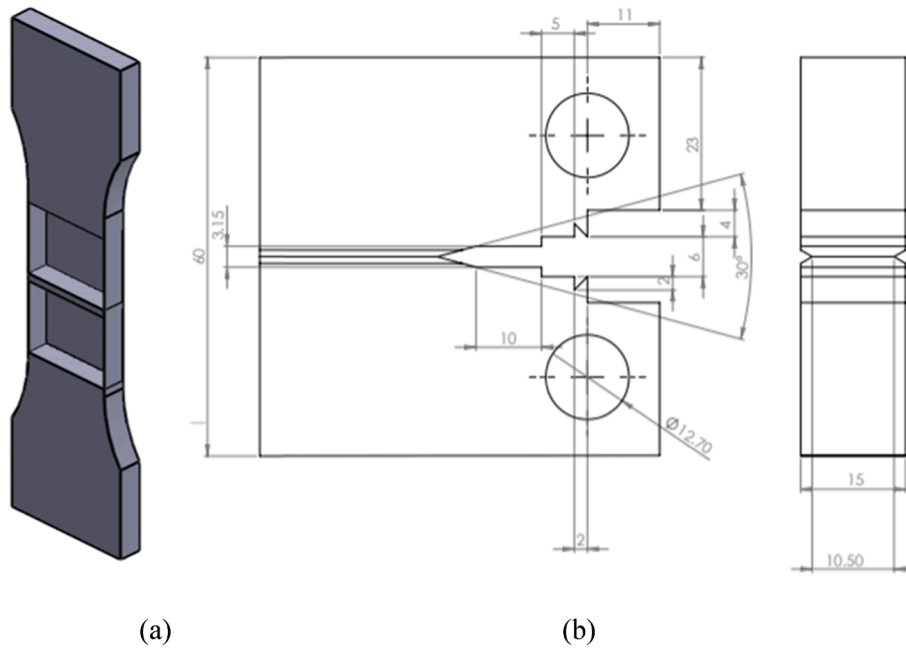


Fig. 6. (a) Schematic illustration of the large-scale dog bone pre-straining samples, (b) engineering drawing of the C(T) specimen notch design for fracture toughness testing (all dimensions are in mm).

Table 3

Fracture toughness test specimen dimensions.

Test ID	W (mm)	B (mm)	B _n (mm)	a _i (mm)
CT-0-1	49.8	15.0	10.3	34.86
CT-0-2	50.0	15.0	10.6	34.51
CT-0-3	50.0	15.0	10.3	34.89
CT-5-1	50.2	15.1	10.3	34.80
CT-5-2	50.0	15.1	10.6	34.86
CT-5-3	50.1	14.9	10.3	34.85
CT-5-4	50.0	15.0	10.5	34.80
CT-10-1	50.1	15.0	10.4	34.88
CT-10-2	50.0	15.0	10.4	34.74
CT-10-3	50.9	15.1	10.4	34.62
CT-10-4	50.0	15.0	10.6	34.84

test methods to side groove the specimens after pre-cracking, for this work the specimens were side grooved prior to pre-cracking to ensure that the starter crack introduced into the specimen through fatigue pre-cracking was perfectly straight. Although the standards recommend side grooving specimens by up to 25%, it was noted in preliminary tests for 0% pre-strained S355 material that 25% side grooving on specimens with the maximum allowable crack size for fracture toughness testing (normalised crack length of $a/W \leq 0.7$) was not sufficient to successfully complete the tests. Therefore, the specimens were side grooved by 30% of the total thickness for this work to facilitate fracture testing. It was ensured that the initial crack lengths at the beginning of fracture toughness tests and after fatigue pre-cracking, a_i , were less than 0.7 of the specimens' width, therefore all fracture toughness tests had a valid initial crack length at the beginning of the tests as recommended by standards. All specimens were pre-fatigue cracked in air at a frequency of 2 Hz and load ratio of $R = 0.1$ to around 10 mm or more from the machined notch with the crack size being calculated by the unloading compliance technique using a clip gauge attached to the samples during the fatigue pre-cracking process. Pre-cracking allows a sharp crack tip to be introduced into the specimens without allowing a significant plastic zone size to be developed ahead of the starter crack tip. The applied load levels during the fatigue pre-cracking process were continuously decreased, as recommended by standards, with the cyclic force maintained below the maximum allowable load limit given by [33,34]:

$$P_m = \frac{0.4Bb_0^2\sigma_y}{2W + a_0} \quad (1)$$

where b_0 is the uncracked ligament defined as the difference between the specimen width and the initial crack length ($W - a_0$).

A hydraulic Instron machine with a load cell capacity of 100 kN was used for both the fatigue pre-cracking and fracture toughness testing. The initial crack lengths after fatigue pre-cracking, a_i , together with the final crack length at the end of each fracture toughness

test, a_f , are presented in Section 2 and Section 4, respectively. Fracture toughness tests were performed by loading and partial unloading (by 30% of each peak load) at specified intervals, on single specimens. The load line displacement was measured using a clip gauge attached to the knife edges which were machined at the crack mouth of the C(T) specimens along the centre of the pin holes. The tests were performed in displacement (i.e. position) control mode with a hold time of 30 s at each peak load with the loading and unloading speed of 1 mm/min.

3.2. Fracture toughness data analysis

For ductile materials, the resistance curves (also known as the R-curves) are built up by plotting the elastic–plastic fracture mechanics parameter J , against the crack extension Δa . According to the ESIS P2-92 standard test method, the J parameter for fracture toughness testing can be calculated by :

$$J = \frac{\eta U}{B_n(W - a_0)} \quad (2)$$

where U is the area under the P vs. LLD curve and η is a geometry dependent constant which can be defined as $\eta = 2 + 0.522(1 - a_0/W)$ for C(T) specimen geometry. The instantaneous crack length in fracture toughness tests can be estimated from the unloading compliance, C , using:

$$\frac{a}{W} = 1.000196 - 4.06319\mu + 11.242\mu^2 - 106.043\mu^3 + 464.335\mu^4 - 650.677\mu^5 \quad (3)$$

where

$$\mu = \frac{1}{[B_{eff}E_wC]^{1/2} + 1} \quad (4)$$

$$B_{eff} = B - (B - B_n)^2/B \quad (5)$$

In (Eq. (4)), E_M is described as the effective Young's Modulus which can be determined from:

$$E_M = \frac{1}{C_0 B_{eff}} \left(\frac{W + a_0}{W - a_0} \right)^2 [2.163 + 12.219 \left(\frac{a_0}{W} \right) - 20.065 \left(\frac{a_0}{W} \right)^2 - 0.9925 \left(\frac{a_0}{W} \right)^3 + 20.609 \left(\frac{a_0}{W} \right)^4 - 9.9314 \left(\frac{a_0}{W} \right)^5] \quad (6)$$

where C_0 is the average compliance determined from the unloadings performed in the elastic regime.

To identify the data points which would be valid for fracture toughness analysis, crack growth limits must be applied. This is achieved by specifying the blunting line slope and constructing parallel exclusion lines at the appropriate offsets as outlined by the standards. ESIS P2-92 provides guidance on characterising the construction lines, in both their slopes and their placements. The equation given by the ESIS P2-92 standard is:

$$J = 3.75\sigma_{ms}\Delta a \quad (7)$$

The construction of an exclusion line helps to define the limit between which the R curve data points are valid and which should be removed. ESIS P2-92 suggests plotting the exclusion lines intersecting the abscissa at 0.1 mm and Δa_{max} which can be calculated using:

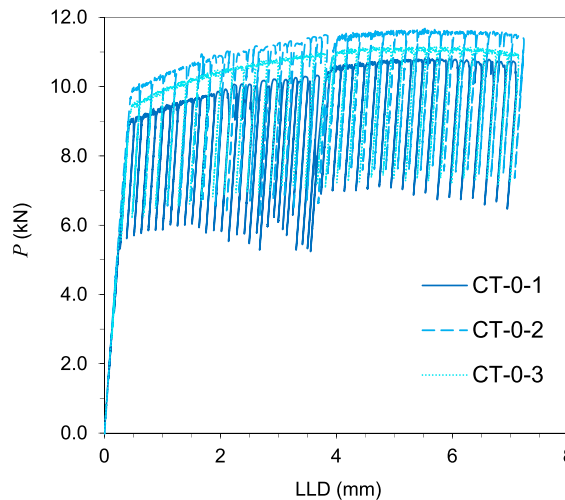


Fig. 7. Comparison of P vs. LLD curves for the as-received specimens.

$$\Delta a_{\max} = 0.1(W - a_0) \quad (8)$$

A line of best fit is plotted through the valid data points falling between the exclusion lines. The intersection with the parallel line at the offset of 0.2 mm indicates the initiation toughness of the material, which is often referred to as J_{IC} .

4. Fracture toughness test results and discussion

4.1. P vs. LLD curves

Fracture toughness tests were performed on as-received (i.e. 0%) and 5% and 10% pre-strained material states at room temperature and the load, P , vs. load line displacement, LLD , curves are shown in Figs. 7, 8 and 9, respectively. The presented curves include the loading and partial unloading data (i.e. by approximately 30% of the individual peak loads) which are used to calculate the unloading compliance and subsequently the instantaneous crack length. The dip in the “ P vs. LLD ” curves midway through testing was due to clip gauge limitation, as the travel range was limited and the test had to be interrupted once the LLD reached 4 mm, and the clip gauge reset to continue the curves. For the as-received material state in Fig. 7, each specimen shows a similar trend and trajectory with regard to peak loads; however, CT-0-1 showed a lower peak load compared to CT-0-2 and CT-0-3. For all three as-received specimens, the peak load starts to drop at the end of the tests. This indicates that despite the relatively large crack mouth opening displacement in the as-received C(T) specimens, which is around 7 mm, the maximum peak load in P vs. LLD response doesn't appear only until the end of the test where the LLD is around 6 mm. This is thought to be due to the high ductility of the as-received S355 material, which makes the maximum load level, $P_{\max}$, appear at relatively large LLD values.

For the 5% specimens' results shown in Fig. 8, all specimens have exhibited an almost identical trend, except CT-5-3 which shows a lower trend compared to the rest. From around 4 mm, all 5% pre-strained specimens show a decrease in peak load towards the end of the tests which indicates that sufficient loadings and unloading were performed beyond $P_{\max}$. In the 10% test batch results shown in Fig. 9 there was more variation in peak load, with specimens CT-10-1 and CT-10-3 showing higher trends. This may be due to the fact that the pre-straining to 10% pre-strain may not have been so uniform in the large-scale dog bone specimens from which the C(T) specimens were extracted. For 10% pre-strain specimens, all specimens show a decrease in peak load from an LLD of around 3.5 mm. Overall, the test results show good repeatability and replication.

In order to directly compare the loading behaviour of the materials with different pre-strain levels, the P vs. LLD data from one specimen per material state (the upper bound trend) are plotted in Fig. 10. This figure shows that the linear slope in the elastic region for all specimens within different pre-strain levels remains the same, which indicates that the elastic properties are almost identical in the as-received and pre-strained materials. It can be seen in this figure that for higher pre-strain levels, a lower load line displacement is required to reach a specific load level. Also seen in Fig. 10 is that as the pre-strain level increases, a higher $P_{\max}$ value is observed in the P vs. LLD response of the material. For example, for the 10% pre-strained material the observed $P_{\max}$ value is around 10% higher than the indicative $P_{\max}$ value of the as-received material which is observed towards the end of the test on 0% material state. This is due to the material hardening effects with pre-strain, which results in higher yield stress, hence greater $P_{\max}$. Finally, seen in Fig. 10, is that the experimentally observed LLD to reach the $P_{\max}$ value in 0%, 5% and 10% pre-strain levels is approximately 6 mm, 4 mm and 3.5 mm, respectively. This means that by increasing the pre-strain level the extent of ductility reduces in the material and subsequently the $P_{\max}$ appears at a lower LLD value.

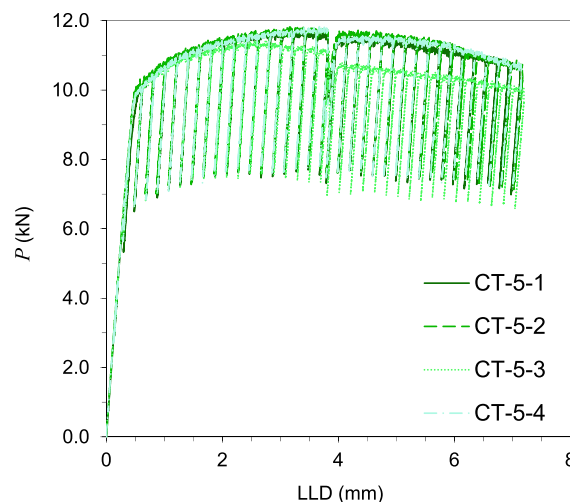


Fig. 8. Comparison of P vs. LLD curves for 5% pre-strained specimens.

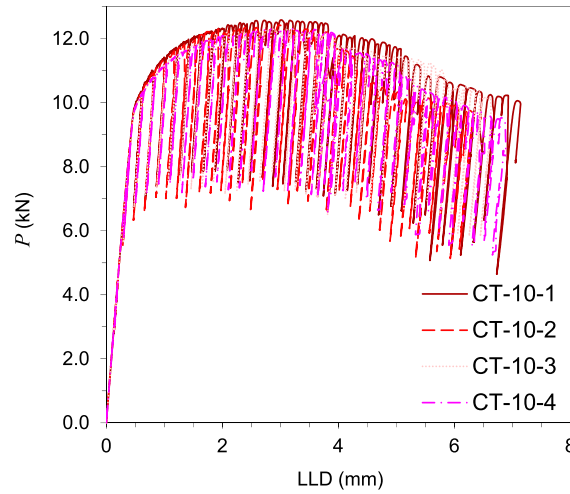


Fig. 9. Comparison of P vs. LLD curves for 10% pre-strained specimens.

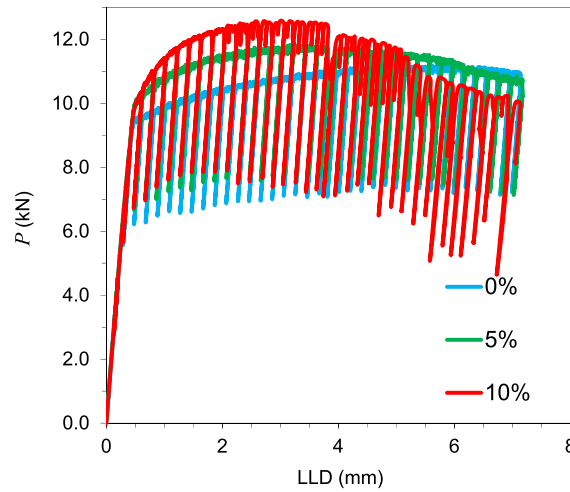


Fig. 10. Comparison of the upper bound P vs. LLD curves for different pre-strain levels.

4.2. Resistance curves

The P vs. LLD data were analysed, using the guidelines detailed in Section 3.3, to generate the R-curves for 0%, 5% and 10% pre-strained specimens in Figs. 11, 12 and 13, respectively. For 0% material state in Fig. 11, the R-curves show a very similar trend with all specimens following a similar slope with some slight variation in values. Also seen in Fig. 11 is that CT-0-1 has exhibited the lowest trend among the as-received material R-curves which is consistent with the P vs. LLD trends shown in Fig. 7. The R-curves for the 5% pre-strained specimens observed in Fig. 12 show good agreement and replicability, with CT-5-1 and CT-5-2 data sets falling upon each other. The lowest R-curve among 5% pre-strained specimens is observed in the test on CT-5-3 which is consistent with the low P vs. LLD curve observed for this specimen in Fig. 8. The results for 10% pre-strained specimens in Fig. 13 show relatively more experimental scatter; however, all results exhibited very similar trends with a clear correlation between J and Δa parameters.

For direct comparison between the R-curves obtained from material with different pre-strain levels, one data set per material state is plotted and compared with other material states in Fig. 14. The steepest curve for each set has been plotted. It can be clearly observed in this figure that an increase in the pre-strain level results in a lower R-curve and therefore the fracture toughness behaviour of S355 material is strongly dependent on the material pre-straining. As seen in Fig. 14, for a given value of Δa the corresponding J value is found to be the highest in 0% as-received material and the lowest in 10% pre-strained material, with the 5% pre-trained material falling in between the two. The results in Fig. 14 show that while for the low values of Δa the resistance curve data points for different material states fall close to each other, it is evident that the difference between J values for pre-strained materials compared to the as-received material state increases by increasing the crack extension. This implies that while plastic pre-straining increases the P_{max} and P vs. LLD trend (see Fig. 10), it has a considerably detrimental effect on the fracture behaviour of the material as shown in Fig. 14. In

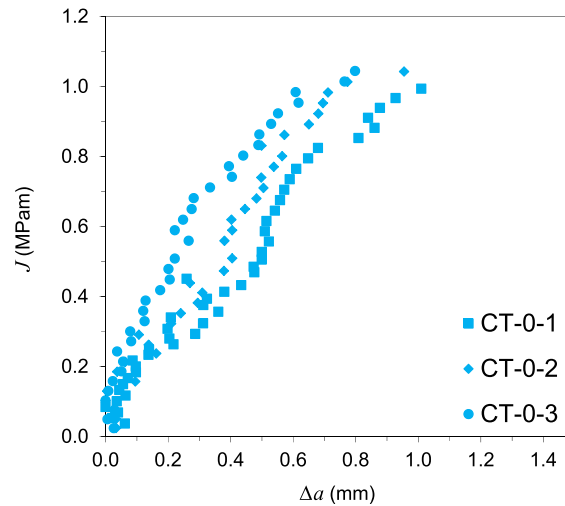


Fig. 11. R-curves for the as-received specimens.

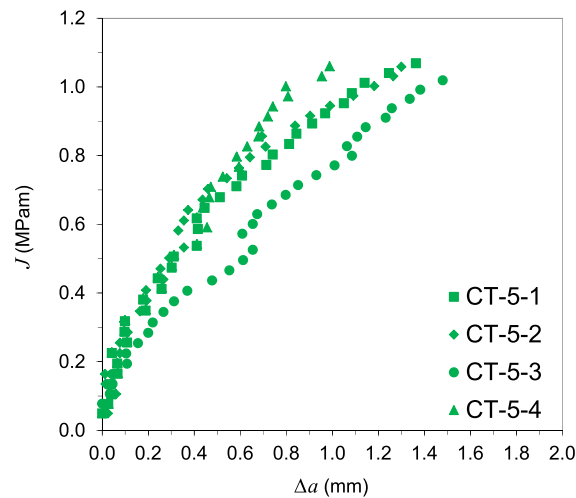


Fig. 12. R-curves for 5% pre-strained specimens.

summary, these observations show that material pre-straining results in a reduction in fracture toughness for S355.

4.3. Fracture toughness results

The R-curves presented in Figs. 11, 12, 13 were further analysed, following the procedure described in Section 3.3, to quantify the fracture initiation toughness values for all the specimens tested in this study. An example of the data analysis process on the R-curves, which includes the construction of a blunting line and exclusions lines, is presented for CT-10-4 specimen in Fig. 15. J_{IC} has been taken as the J value intersecting at the 0.2 mm exclusion line. The equations of the line of best fits to the valid data points for each of the specimens examined in this study are collectively presented in Table 4. Also included in this table are the Δa_{max} and J_{IC} values for specimens with different pre-strain levels. As seen in Table 4, the fracture initiation toughness values for 0%, 5% and 10% pre-strained materials are found to range within 0.76–1.02, 0.44–0.86 and 0.65–0.78, respectively. The test results show that the average J_{IC} values for the as-received, 5% and 10% pre-strained material states are 0.86 MPa·m, 0.79 MPa·m and 0.71 MPa·m, respectively. This indicates that an increase in the plastic pre-strain level in S355 material leads to a reduction in the fracture initiation toughness value. It can be noted that the energy required to initiate the crack is higher in 0% pre-strain specimens compared to 5% and 10% pre-strained specimens. This is because the as-received material has a higher ductility and lower yield stress, therefore a higher level of energy is required for fracture crack initiation. The results show that on average, tensile pre-straining to 10% reduces the J_{IC} fracture toughness value by around 15% to that observed in the as-received material, therefore this effect should be quantified and accounted for in life assessment of offshore wind monopiles.

Furthermore, the fracture toughness results were re-evaluated by calculating the J_{IC} values using an offset line of 0.5 mm to account

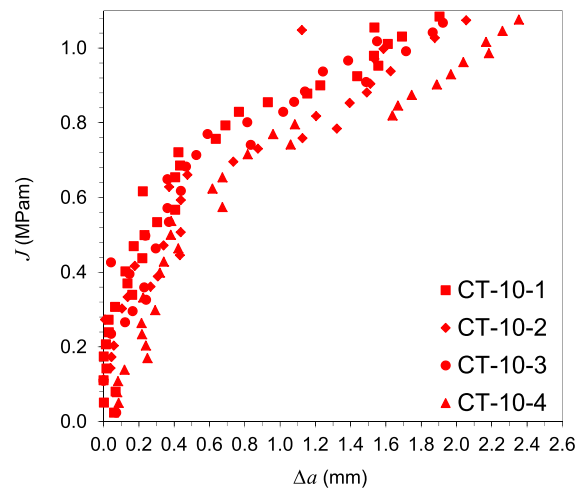


Fig. 13. R-curves for 10% pre-strained specimens.

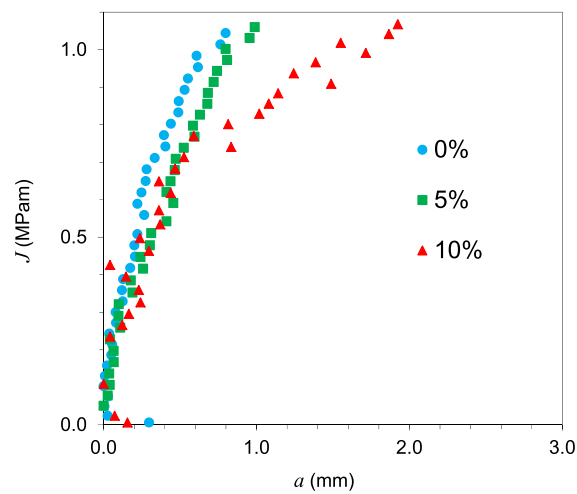


Fig. 14. Comparison of R-curves for different pre-strain levels.

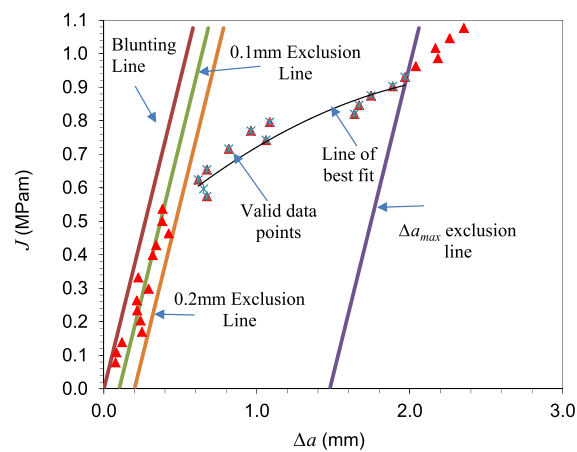


Fig. 15. An example of the fracture toughness analysis using the R-curve generated for CT-10-4 specimen.

Table 4 J_{IC} test results for all specimens with different pre-strain levels.

Test ID	Δa_{max} (mm)	J_{IC} (MPam)	Line of best fit equation
CT-0-1	2.70	0.76	$y = -1.5401\Delta a^2 + 3.0792\Delta a - 0.5776$
CT-0-2	1.57	0.80	$y = -1.2886\Delta a^2 + 2.4908\Delta a - 0.1567$
CT-0-3	1.51	1.02	$y = -0.7322\Delta a^2 + 1.4187\Delta a + 0.3695$
CT-5-1	1.50	0.72	$y = -0.1959\Delta a^2 + 0.8514\Delta a + 0.2808$
CT-5-2	1.57	0.80	$y = -0.0718\Delta a^2 + 0.5016\Delta a + 0.517$
CT-5-3	1.51	0.44	$y = -0.0466\Delta a^2 + 0.6807\Delta a + 0.1335$
CT-5-4	1.54	0.86	$y = -1.925\Delta a^2 + 3.7828\Delta a - 0.8102$
CT-10-1	1.47	0.78	$y = 0.0583\Delta a^2 + 0.1005\Delta a + 0.6939$
CT-10-2	1.55	0.69	$y = -0.0299\Delta a^2 + 0.3554\Delta a + 0.4608$
CT-10-3	1.46	0.74	$y = -0.0163\Delta a^2 + 0.2955\Delta a + 0.5651$
CT-10-4	1.48	0.65	$y = -0.0836\Delta a^2 + 0.4386\Delta a + 0.3665$

for scatter in fracture toughness values. Table 5 gives a summary of the averages of the findings. From the table, it can be seen that even after accounting for scatter, the correlation of a reduction in fracture toughness with increased pre-straining still holds true.

4.4. Fractography

Upon completion of the fracture toughness tests, the specimens were soaked in liquid nitrogen for 5 min and then broken open under monotonic loading conditions. Fractography analysis was conducted by taking high resolution macroscopic pictures of the fracture surfaces of all specimens after testing in order to measure the actual crack lengths prior to and after fracture toughness testing. Crack lengths were measured using ImageJ software, and the crack extensions reported are an average of values across the fracture surface. An example of the fracture surfaces for 0% can be seen in Fig. 16. From the fracture surface, three zones can be identified: the zone of fatigue pre-cracking, fracture toughness crack growth and fast fracture during specimen break open (see Fig. 16). From the fracture surface, there is symmetric crack propagation in the pre-cracking region, and this suggests good alignment in the test set-up. Table 6 shows the crack length estimations taken from the unloading compliance for the final crack length, at the end of the fracture toughness tests, compared to the measured final crack lengths on the fracture surface. The percentage error between each has also been calculated. The results show that there is a good agreement between the measurements taken from the unloading compliance compared to the actual measurements taken in the fractography analysis. This is determined by the percentage error between the two, which is in general less than 7%. Due to the fact that the percentages of error for a_f values are very small, it can be concluded that there is relatively good agreement overall between the unloading compliance and the actual measurements. Therefore, it can be concluded with confidence that the R-curves and quantified J_{IC} values are reliable.

4.5. Pre-straining effects on structural integrity assessments

In the offshore wind industry, the location of cracks may occur at the weld toes between plates, or around cable holes for electrical cabling of monopiles. It is to be understood that during the induced material pre-straining during the manufacturing process, the strain distributions through the thickness of the plates are not uniform, and instead a progression from tensile strains on the outer surface to compressive strains on the inner surface would exist [22]. Comparison of the results obtained from the present study with those available in the literature show that material pre-straining affects the fracture and mechanical properties regardless of whether the pre-strains are tensile or compressive, as both pre-straining directions have been found to increase the yield stress and reduce the fracture toughness of the material [29].

The results obtained from the present study highlight the importance of pre-straining effects in the structural integrity assessment of offshore wind monopiles. In order to find a correlation between the fracture toughness and the tensile pre-strain level, the data obtained from the tests performed in the current study are further analysed in Fig. 17. This figure graphically presents the correlation between J_{IC} values and yield strength with the plastic pre-strain level, ϵ_p . It can be seen in this figure that as the pre-strain level increases, the yield strength increases due to strain hardening mechanisms. Therefore, the material decreases in ductility and thus a decrease in J_{IC} values appears in the material's behaviour. So, it can be seen that within the range of tensile pre-strains considered in this study there is a direct correlation between the average fracture toughness value and the level of pre-tensioning specifically for steel grade S355G10+M which can be empirically described as:

$$J_{IC} = -0.0145\epsilon_p + 0.8618 \quad (9)$$

Table 5

Fracture toughness values using 0.5 mm offset line.

Pre-strain (%)	J_{IC} (MPam)
0	1.01
5	0.95
10	0.81

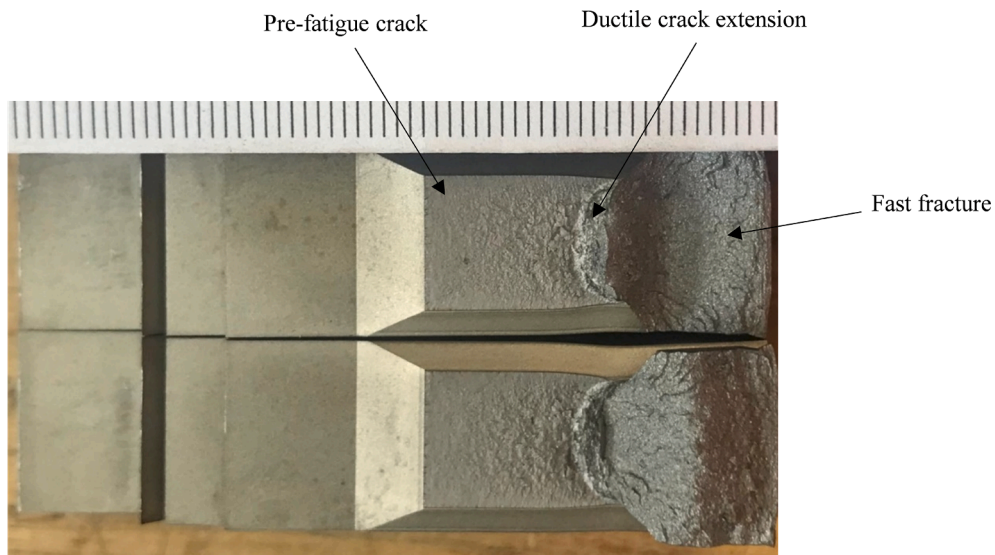


Fig. 16. Fracture surface of an as-received specimen (the division lines in the scale bar are spaced with 1 mm distance).

Table 6

Comparison of crack length estimates and measurements.

Specimen ID	a_i – from the unloading compliance (mm)	a_i – from the fracture surface (mm)	% error in estimation of a_i	a_f – from the unloading compliance (mm)	a_f – from the fracture surface (mm)	% error in estimation of a_f
CT-0-1	12.17	12.39	1.74	36.22	37.11	2.40
CT-0-2	12.54	11.87	5.61	35.29	36.81	4.13
CT-0-3	12.15	12.16	0.07	35.81	37.10	3.48
CT-5-1	12.31	12.53	1.81	36.59	38.29	4.44
CT-5-2	12.42	11.77	5.48	35.65	37.42	4.73
CT-5-3	12.18	12.24	0.52	36.54	37.91	3.61
CT-5-4	12.21	11.84	2.14	35.62	36.31	1.90
CT-10-1	12.18	12.59	3.27	37.33	38.64	3.39
CT-10-2	12.43	11.96	3.87	36.90	39.32	6.15
CT-10-3	12.16	12.59	3.46	37.36	38.49	2.94
CT-10-4	12.11	12.36	2.02	36.60	38.39	4.66

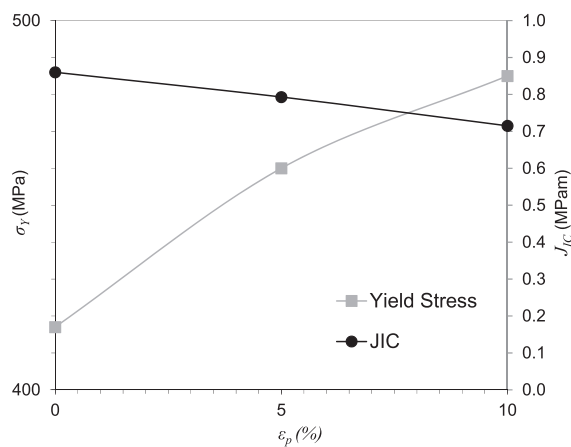


Fig. 17. Effects of pre-strain level on yield stress and J_{IC} values.

The proposed empirical relationship could enable monopile fabricators and designers in the offshore wind industry using this specific steel grade (S355G10+M) to gain a more accurate design life and realistic structural integrity analysis by considering the plastic strain levels introduced into the monopiles during the fabrication process. These obtained fracture toughness values and proposed empirical relationship can be used in future work to perform further analysis using failure assessment diagrams to estimate the critical crack length beyond which failure would occur in monopiles, and thus enable appropriate inspection and maintenance schedules to avoid catastrophic failures in the offshore wind turbine foundations. However, this relationship needs to be tested across a larger database of experimental results than what is presented in this paper. These results will be used in the future to develop and validate a finite element model to predict the pre-straining effects on fracture behaviour of structural steels to achieve a more accurate remaining life estimation of offshore structures. It must also be pointed out that C(T) specimens, especially those with side grooves, experience a high degree of crack tip constraint. Therefore, the fracture toughness results presented in this paper are a highly conservative assessment of the structural integrity of a monopile structure. Further work needs to be undertaken to look into the transferability of the constraint parameter from C(T) testing specimens to monopile structures. The trend in reduction of J_{IC} as a result of plastic pre-straining can be used in the structural integrity assessment of fatigue cracks starting from the toes of girth welds as well as cable holes present in offshore wind turbine monopile structures, as the results from this study can be used in failure assessment diagrams for defect assessment. As fracture toughness values are inputted into failure assessment diagram criteria, a change in fracture toughness could make a significant impact on the defect assessment of an offshore wind monopile structure.

5. Conclusions

Material pre-strain was introduced into S355 structural steel samples by performing interrupted tensile tests to stress levels corresponding to 5% and 10% plastic strain at room temperature. C(T) specimens were fabricated from the pre-strained material and fracture toughness tests were conducted at room temperature. From fracture surface analysis it was concluded that there was good alignment in the test set-up, and that there was good agreement between the estimated crack length from unloading compliance and the actual crack length measurements. P vs. LLD plots of each pre-strained material were analysed and from the results it could be seen that the peak load increased with the pre-strain level, with a decrease in load line displacement value. This is due to the decrease in ductility and increase in the yield stress of the material as a result of pre-straining. From the R-curve results it was observed that pre-straining decreases the J_{IC} fracture toughness by up to 15% compared to the as-received material. This is due to the material hardening and thus a reduced ability to absorb energy due to the increase in dislocation density. An empirical formula was determined to correlate the yield stress and fracture toughness values with the pre-strain level for S355G10+M structural steel, which could improve the structural integrity assessment on offshore wind turbine monopile foundations which are subjected to plastic pre-strains during the fabrication process; however, further investigation needs to be conducted in future work to improve the life assessment of monopiles made of a wider range of structural steels.

Declaration of Competing Interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Acknowledgements

This work was supported by grant EP/L016303/1 for Cranfield, Oxford and Strathclyde Universities, Centre for Doctoral Training in Renewable Energy Marine Structures - REMS (<http://www.rems-cdt.ac.uk/>) from the UK Engineering and Physical Sciences Research Council (EPSRC).

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Material pre-straining effects on fracture toughness variation in offshore wind turbine foundations

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2021-06-15

Elsevier

<https://doi.org/10.1016/j.engfracmech.2021.107844>

Anandavijayan S, Mehmanparast A, Brennan F, Chahardehi A. (2021) Material pre-straining effects on fracture toughness variation in offshore wind turbine foundations. Engineering Fracture Mechanics, Volume 252, July 2021, Article number 107844