



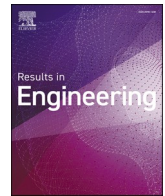
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Research paper

Determination of initial geometric imperfections in the flanges of stainless steel corrugated web I-Girders using distributed optical fibre sensing

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ABSTRACT

Geometric imperfections significantly influence the local flange resistance of plated structures, including corrugated web I-girders; however, reliable methods for their characterization and modelling remain limited. This study investigates different approaches for representing initial imperfections and their influence on the moment resistance of corrugated web girders through a full-scale four-point bending test on a stainless steel specimen. Prior to testing, the central corrugated web segment was captured using 3D scanning to obtain the as-built geometry, while distributed optical fibre sensing (DOFS) sensors recorded strain distributions on the upper surface of the top flange during loading. The scanned geometry was incorporated into a geometrically and materially nonlinear finite element (FE) model, which was validated against the experimental load–displacement response and measured strain fields. In parallel, simplified shell models employing eigenmode-based imperfections with amplitudes derived from the 3D scan, DOFS measurements, and the Eurocode recommendation of $c_f/50$, where c_f denotes the maximum flange outstand, were evaluated. The results demonstrate that the scan-based solid model accurately reproduces both the global and local structural responses, whereas shell models using imperfection amplitudes directly obtained from the 3D scan or the Eurocode recommendation significantly underestimate the ultimate resistance. Improved predictions were achieved by isolating the local flange imperfections associated with the corrugation from the global initial imperfections. Based on the observed correlation between distributed strain profiles and local geometric deviations, an analytical expression is proposed for estimating local imperfection amplitudes from DOFS measurements. Although demonstrated for the investigated specimen, the results indicate that DOFS provides a practical means of estimating representative imperfection amplitudes for advanced FE modelling and highlight that local undulating flange imperfections associated with the corrugated web govern the flange local buckling resistance.

1. Introduction

Flange local buckling in corrugated web girders (CWGs) has emerged as a critical design consideration owing to the non-uniform stress distribution induced by the corrugated web, which differs from that of conventional flat web girders. In flat web girders, the web contributes to both shear and bending resistance, thereby participating in the distribution of longitudinal stresses across the section. In contrast, the corrugated web, owing to its folded geometry, carries shear efficiently but exhibits negligible axial stiffness in the longitudinal direction, a phenomenon commonly referred to as the *accordion effect*. As a result, bending stresses are transferred almost entirely to the flanges, making

the compression flange the principal load-carrying component under bending.

At the same time, the corrugated geometry introduces additional complexities that are absent in flat web girders. These include non-uniform support conditions along the flange, transverse flange bending induced by web eccentricity, and localized stress concentrations at fold junctions. Collectively, these effects alter the stress state in the flanges, which in turn has a significant influence on the flange buckling response and distinguishes the behaviour of CWGs from that of conventional flat web girders [1–3].

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1.1. Background

Despite its importance, flange local buckling in corrugated web girders has received relatively limited attention in experimental research compared to web buckling under shear. The most comprehensive experimental evidence remains the test program conducted in 2017, in which large-scale girders subjected to four-point bending consistently failed by the compression flange local buckling [4]. That study identified distinct buckling patterns as a function of geometric parameters and highlighted the strong influence of corrugation geometry on the development of initial imperfections.

Subsequent experimental investigations have shown that, with reduced lateral restraint, the governing failure mode may shift from flange local buckling (FLB) to lateral-torsional buckling (LTB), underscoring the sensitivity of CWGs to boundary conditions and overall structural configuration [5]. In addition, studies on bending-shear interaction indicate that shear can influence the stress state in the flange through transverse bending effects and stress redistribution, even in cases where ultimate failure remains governed by bending resistance [6].

Parallel to experimental efforts, numerical investigations have played a central role in advancing the understanding of FLB in CWGs. The finite element framework introduced by [7] established a robust finite element modelling (FEM) procedure that has since been widely adopted and extended for parametric studies and design-oriented developments. Numerical investigations have consistently shown that the buckling resistance of the compression flange is highly sensitive to geometric parameters, including the flange subpanel aspect ratio, corrugation geometry, and the flange-to-web thickness ratio. These parameters govern not only the stiffness distribution but also the interaction between local and global instability modes [8].

Material effects constitute another important dimension of the problem. While most studies have focused on conventional carbon steel, recent numerical work on duplex stainless steel demonstrates that the general FEM-based design framework can be extended to alternative materials. However, such extensions require careful recalibration of buckling curves and resistance models. These findings indicate that material nonlinearity and stress-strain characteristics significantly influence flange buckling behaviour and should be explicitly incorporated into design approaches, rather than assumed to be directly transferable from carbon steel-based formulations [9].

EN 1993-1-5:2024 serves as the primary European standard for the design of CWGs. In this revised edition, dedicated provisions for corrugated web girders have been incorporated into a separate chapter, replacing the previous Annex D of earlier editions. Consequently, the standard provides the principal code-based framework for the design of CWGs in Europe. However, an increasing body of research has demonstrated that the current EC design model can lead to unsafe predictions, particularly for slender compression flanges, with similar shortcomings identified for mono-symmetric girders and more advanced materials such as duplex stainless steel [7–9]. Conversely, other investigations indicate that the same design model may be overly conservative, for example due to the neglect of web bending contribution in compact, laterally restrained members [10]. This dual inconsistency, unsafe in some cases and overly conservative in others, highlights that current design model in Eurocode is not yet reliable as a stand-alone tool across the full spectrum of geometries, materials, and restraint conditions [4]. The situation is even less mature outside Europe, where specifications such as AASHTO LRFD, AISC 360, and CSA S16 do not yet provide comprehensive design rules for CWGs. As a result, designers often revert either to simplified plate-girder analogies or to project-specific advanced analysis. The implication is clear: while code-based design provides a useful baseline, it is insufficient to ensure safety and accuracy in many practical cases, thereby motivating the increasing reliance on FEM-based design approaches.

In contrast to the lack of consistency in current design guidance, a

relatively well-established agreement has emerged regarding FEM-based design strategies. Recent flange buckling studies almost universally adopt shell-based finite element models, typically starting with a linear buckling analysis to extract critical mode shapes, followed by geometrically nonlinear analysis that incorporates initial imperfections. More comprehensive investigations further include material nonlinearity and, where feasible, residual stresses [7]. Validation procedures have also evolved beyond single-value comparisons into a multi-objective approach, in which ultimate load, load-deflection response, strain distributions, and failure mode shapes are evaluated simultaneously. A 2025 systematic review confirms that this integrated workflow has effectively become standard practice across the CWGs' literature [11].

Within this FEM framework, one parameter stands out as particularly critical: *initial imperfections*. Their importance is consistently highlighted across numerical, experimental, and manufacturing-oriented studies. The 2017 FLB finite element study [7] placed imperfection sensitivity at the core of its design development, while the 2022 LTB experiments [12] explicitly identified imperfections as a key driver of structural response. In addition, the 2020 manufacturing-simulation studies showed that CWGs develop residual stress fields and geometric imperfections that differ qualitatively from those of flat-web girders, with bending-type residual stresses dominating in the corrugated web and geometric imperfections often having a greater influence on buckling resistance than residual stresses themselves [13].

Previous studies have shown that both the shape and the magnitude of geometric imperfections are decisive parameters. Regarding the imperfection shape, the prevailing modelling approach is to represent imperfections using either a single buckling mode or a combination of different eigenmode shapes. However, real CWGs rarely exhibit a single pure mode. Fabrication processes introduce a combination of local flange distortions, global member curvature, and corrugation-induced irregularities, resulting in imperfection patterns that are more complex than a single eigenmode representation. For this reason, more advanced studies sometimes employ superpositions of multiple buckling modes or use directly measured imperfection fields, approaches that generally enhance realism but come at the expense of increased modelling complexity.

The magnitude of imperfections is equally influential and often exhibits a highly nonlinear effect on the predicted resistance. In practice, imperfections are commonly scaled using simplified rules based on plate thickness or member dimensions, or by adopting generic values derived from flat-web girder design. However, for CWGs, such approaches can be misleading. Research consistently shows that imperfection sensitivity is strongly case-dependent, particularly for slender compression flanges, and that even small variations in the assumed amplitude can lead to significant differences in ultimate capacity.

These observations underline a broader conclusion: FEM-based design is essential for the reliable assessment of CWGs, but its accuracy depends critically on informed modelling choices, particularly in the representation of initial imperfections. The available experimental database on flange local buckling remains limited, especially for non-conventional materials. These limitations point to several pressing research needs. There is a need to establish reliable imperfection models suitable for use in geometrically and materially nonlinear analysis with imperfections (GMNIA), together with further experimental validation of flange buckling resistance for advanced materials such as duplex stainless steel and high-strength steel.

The importance of these aspects is further emphasized by the introduction of Eurocode 3: EN 1993-1-14, which formally incorporates finite element-assisted design into the Eurocode framework. While this development creates new opportunities for more efficient and optimized structural design, it also increases the responsibility placed on engineers to ensure that nonlinear FE models adequately represent the governing physical behaviour, including the selection and implementation of appropriate initial imperfections for different buckling phenomena.

Consequently, improving the understanding of imperfection sensitivity in CWGs is essential for enabling safe and reliable FE-assisted design in accordance with modern code provisions and for bridging the gap between advanced numerical research and practical engineering applications.

1.2. Aim and research approach

The present paper aims to provide deeper insight into how initial imperfections should be measured and modelled for use in finite element method-based design for flange local buckling in corrugated web girders. To this end, a duplex stainless-steel girder with a central segment incorporating a corrugated web plate was fabricated and tested under four-point bending (ref. Figs. 1 and 4)

Prior to testing, the central segment was 3D scanned to capture the actual geometric imperfections arising from the manufacturing process. This scanned geometry was then implemented in a finite element (FE) model and subjected to pure bending, allowing direct comparison between numerical and experimental results for validation purposes. Since the use of 3D-scanned geometries in FEM is not a common practice, designers typically account for imperfections by performing a linear

buckling analysis to identify critical mode shapes, which are then scaled based on measured or recommended maximum imperfection amplitudes.

An idealized CAD-based geometry was first modelled, and an eigenvalue buckling analysis was conducted to extract the relevant mode shapes. These mode shapes were then introduced as initial imperfections in a geometrically and materially nonlinear analysis scaled to three different imperfection amplitude (ref. 2.3.2). The resulting numerical predictions were compared with the experimental results (ref. Fig. 18)

Based on this comparison, as well as further analysis of strain measurements using DOFS and a sensitivity study on the pattern and magnitude of initial imperfections in the FE models, an analytical relationship is derived enabling estimation of the maximum amplitude of initial imperfections from fibre-optic strain measurements, allowing for more accurate simulation of experimental behaviour in FEM analyses.

Optical frequency domain reflectometry (OFDR)-based distributed sensing has demonstrated strong practical value in the testing of steel components, particularly where detailed spatial characterization of structural behaviour is required. Nevertheless, the literature emphasizes that the successful application of such systems depends not only on the

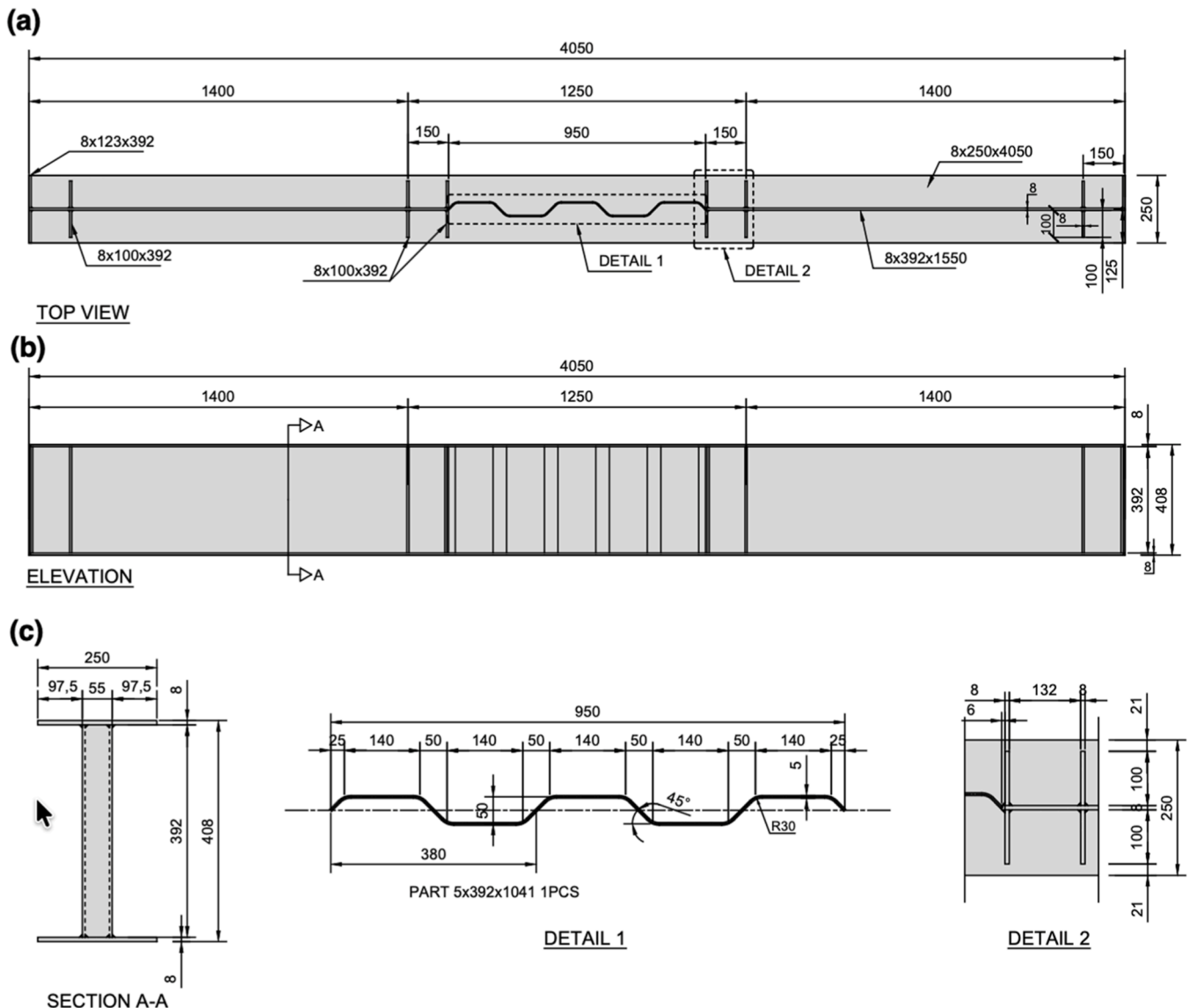


Fig. 1. Beam geometry. (a) Top view; (b) Elevation; (c) Detail of the corrugation, typical section and stiffeners. Measurements in mm.

sensing principle itself but also on several critical implementation factors. Adhesive selection plays a central role, alongside recurring challenges such as bond quality and strain transfer, interface debonding or slip, temperature cross-sensitivity, and baseline drift, especially during long-duration tests, all of which directly affect data reliability. Furthermore, the use of distributed sensing at sampling rates of tens to hundreds of hertz introduces substantial demands in terms of data management and processing [14–17].

To the best of the authors' knowledge, this study is the first to employ DOFS measurements for the estimation of initial geometric imperfections in steel structures. Unlike conventional approaches, which rely primarily on direct geometric measurements or assumed imperfection amplitudes, the proposed methodology establishes an analytical relationship that enables the initial local flange imperfections to be estimated directly from distributed strain measurements obtained at low to moderate load levels. For the investigated specimen, the estimated imperfections are verified through comparison with the independently measured 3D-scanned geometry, while the structural response is further assessed using experimentally validated nonlinear finite element analyses. These results demonstrate the feasibility of the proposed methodology for the investigated specimen. However, verification for a single specimen should be distinguished from general validation of the methodology. The need for further independent validation limits the generality of the present conclusions but does not affect the demonstrated agreement between the DOFS-based estimates, 3D-scanned geometry, and numerical and experimental results obtained for the investigated specimen. Further experimental and numerical studies covering additional corrugated web girder configurations are therefore required to establish the broader applicability and robustness of the proposed methodology.

Although the investigated specimen was fabricated from duplex stainless steel, the primary objective of this study is the characterization and modelling of initial geometric imperfections rather than the assessment of material-specific behaviour. The tested specimen was selected from a broader experimental programme that also included a carbon steel corrugated web girder instrumented with DOFS. Consequently, the proposed methodology is expected to be applicable to both stainless steel and carbon steel girders and, more generally, to thin-walled structures where local geometric imperfections influence the structural response.

The overall methodology of the current research is detailed in Section 2; the results of the experimental and numerical finite element studies are presented and discussed in Section 3 and the derived analytical formulation for the purpose of estimating the magnitude of initial imperfections are presented in Section 4. The final conclusions are presented in Section 5.

2. Methodology

2.1. Description of experiments

2.1.1. Beam geometry

A duplex stainless-steel I-beam was fabricated for this study to assess the impact of web corrugation on the local buckling capacity of the flange. The girder measures 4050 mm in length and incorporates a corrugated web in the central segment, spanning 950 mm. Featuring an I-shaped cross section, the girder was assembled by welding its conforming elements. To mitigate local web failure during testing, stiffeners were installed at critical locations, i.e. under the loading points and above the supports. Fig. 1(a and b) presents elevation and top views of the complete girder, while Fig. 1(c) provides detailed illustrations of the corrugation, a typical cross section, and the configuration of the stiffeners.

2.1.2. Material characterisation

Lean duplex EN 1.4162 stainless steel material properties were

assessed by testing dog-bone shaped coupons under monotonic tensile loads using an MTS Universal Testing machine until failure. Coupons followed ISO 6892 specifications, see Fig. 2. Table 1 lists mean measurements from the three sections indicated in Fig. 2 within the reduced area. Both total machine displacement and bar deformation were recorded; element deformation was measured at the centre with a 50 mm gauge length extensometer.

The tensile test results demonstrated the typical behaviour associated with duplex stainless steel. Unlike carbon steel, which usually exhibits a distinct yield stress and plateau prior to hardening, duplex stainless steel showed no clear yielding point. Instead, a continuous transition was observed between the quasi-elastic and plastic phases, as illustrated in Fig. 3(a). Stress calculations were based on the mean area values provided in Table 1, resulting in the engineering stress-strain curves presented in Fig. 3(a and b). For the subsequent beam response FE modelling described in 2.3, a constitutive material model is required. Consequently, the test data was converted into true stress-strain curves and further streamlined into a representative simplified curve, see Fig. 3(b), which was then used as input in FEM. True stress values were calculated from engineering measurements using Eqs. (1) and (2).

$$\sigma_{true} = \sigma_{eng} \cdot (1 + \epsilon_{eng}) \quad (1)$$

$$\epsilon_{true} = \ln(1 + \epsilon_{eng}) \quad (2)$$

A Young's modulus of 200 GPa, a yield strength of 460 MPa, and an ultimate tensile strength of approximately 700 MPa were assumed, as illustrated in Fig. 3. Material nonlinearity was modelled in ABAQUS using an isotropic plasticity constitutive model with strain hardening.

2.1.3. Test setup and sensor instrumentation

The beam was simply supported on rollers and subjected to four-point bending. To ensure global stability throughout testing, lateral displacement at the load application points was restrained by chains attached to a supporting frame on both sides, ensuring that any movement in one direction was counteracted by the opposing chain, see Fig. 4(a). The load points and supports were placed so the beam was divided in three equal spans of 1250 mm, i.e. two shear spans and one intermediate span with constant moment, as illustrated in Fig. 4(a and b). Monotonic loading was carried out by means of the two jacks. One of the jacks was controlled by displacement-control utilising a closed-loop feedback system at a rate of 1.2 mm/min, with the second jack matching the load of the first. The load was applied monotonically until failure occurred.

During testing, load values were captured and stored every second using a load cell positioned beneath the actuator. Specimen vertical deflection was measured via a Linear Variable Differential Transformer (LVDT) placed at the beam's midpoint, with a recording interval of one measurement per second. To obtain a more detailed assessment of the central region of the beam, strains along the beam's upper surface were monitored using a distributed optical fibre sensor. In the present study, an acrylic-coated fibre with a nominal diameter of 125 μm was affixed to

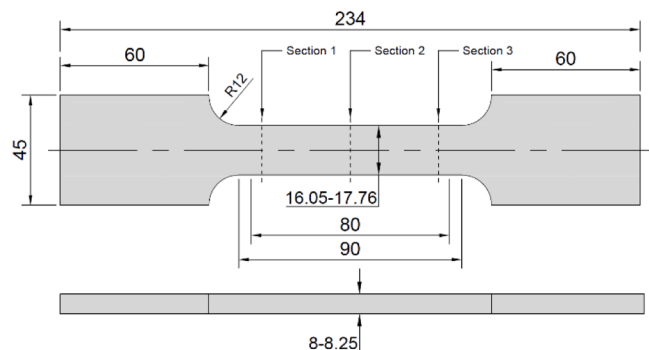


Fig. 2. Dog-bone geometry for material testing.

Table 1
Geometrical description of the dog-bone specimens.

	Mean width [mm]	Mean thickness [mm]	Area [mm ²]
Specimen 1	17,71	8,09	143,24
Specimen 2	17,63	8,05	141,89
Specimen 3	17,58	8,04	141,31
Specimen 4	17,57	8,04	141,32
Specimen 5	16,02	8,16	130,66

the specimen as depicted schematically in Fig. 4(b) using cyanoacrylate-based adhesive. This choice enables high spatial resolution and flexible routing on steel components; further, this coating has good bond properties with the glass fibre which introduces very little coating-dependent strain transfer effects. Similarly, cyanoacrylate bonding is widely employed due to its rapid curing and high strain fidelity under controlled laboratory conditions, although its relatively brittle nature may increase the risk of premature debonding or localized anomalies compared to more compliant bonding systems [18]. Data acquisition was performed with the Optical Distributed Sensor Interrogator (ODiSI) 6108 series from Luna Inc. The interrogator was configured to its highest spatial resolution between measuring points, namely 0.65 mm. Finally, the sampling rate was set to several recordings per second, which is generally appropriate for static or quasi-static testing, providing a balance between signal quality and manageable data rates.

2.2. Measurement of initial imperfection through 3D laser scanning

Prior to testing, the area between the stiffeners, highlighted in Fig. 4(b) – elevation, was 3D scanned to identify any initial geometric imperfections resulting from manufacturing. The scanned region was approximately 950 mm long, 400 mm high, and 250 mm wide. Before scanning, the maximum flange deflection was also checked using a set square and a ruler. This preliminary check confirmed the presence and approximate magnitude of the flange deflection, but it was not systematically documented and was therefore not used as a quantitative validation measurement. The scanning process used a HandySCAN 700™ portable laser scanner from Creaform, with a manufacturer-specified accuracy of up to 0.030 mm, a measurement resolution of up to 0.050 mm, and a volumetric accuracy of $(0.020+0.060 L)$ mm, where L is the characteristic measured length in metres. Considering the maximum dimension of the scanned region, $L = 0.95$ m, the corresponding nominal volumetric accuracy was approximately 0.08 mm.

Immediately before scanning, the standard calibration procedure prescribed by the manufacturer was performed using the calibration object supplied with the scanner. Data acquisition was carried out using

Creaform VXEelements in point-cloud mode. The measurements were continuously registered by the software during acquisition, and the resulting point cloud was exported without first generating a polygonal mesh. The exported point cloud, illustrated in Fig. 5, was subsequently processed in MATLAB to remove noise and isolated points. A triangular surface mesh representing the inspected region was then reconstructed from the cleaned point cloud, and minor remaining holes were closed. Following the approach outlined by [19], the processed surface was later transformed into a solid ready for meshing in the FEM software.

Assuming that the stiffeners delimiting the corrugated region of the beam established the boundaries for initial imperfections in the corrugated region, it is possible to construct a view of the top surface of the upper flange and thereby observe the vertical deviations of various points on the flange relative to these stiffeners. Fig. 6(b) illustrates the spatial distribution of the vertical deviation, revealing the influence of the corrugation and indicating that points located farther from the web are more affected by initial imperfections. The same figure suggests that the maximum local imperfection occurs at the middle bottom side, which features only two corrugations instead of three. Fig. 6(a) and Fig. 6(c) present longitudinal profiles of the relative vertical variations measured at the five positions where the DOFS were glued. Consistent with previous observations, DOFS position 5 exhibited the largest imperfection (approximately 6 mm), as it is positioned at the outermost location on the critical side of the web.

2.3. Numerical modelling

2.3.1. 3D scanned geometry-based FEM

As the first step of the finite-element study, the solid geometry derived from the 3D scanning data was imported into ABAQUS to conduct a fully material and geometrically nonlinear analysis using the arc-length Riks method. The central corrugated segment of the girder was discretized using C3D10 quadratic tetrahedral solid elements with a maximum element size of 5 mm, as shown in Fig. 7(b). Mesh convergence was assessed to ensure that the adopted discretization provided sufficiently converged results. Further mesh refinement resulted in negligible changes in the predicted structural response while substantially increasing the computational cost.

The quality of the scan-based mesh was also evaluated. Of the 413,474 elements in the model, 212 elements were identified by ABAQUS in the WarnElemDistorted set because their isoparametric angles were outside the suggested limits or their tetrahedral quality measure was poor. These elements represent approximately 0.051% of the total mesh. This small proportion of flagged elements did not prevent convergence of the geometrically and materially nonlinear analysis or result in numerical singularities that compromised the solution.

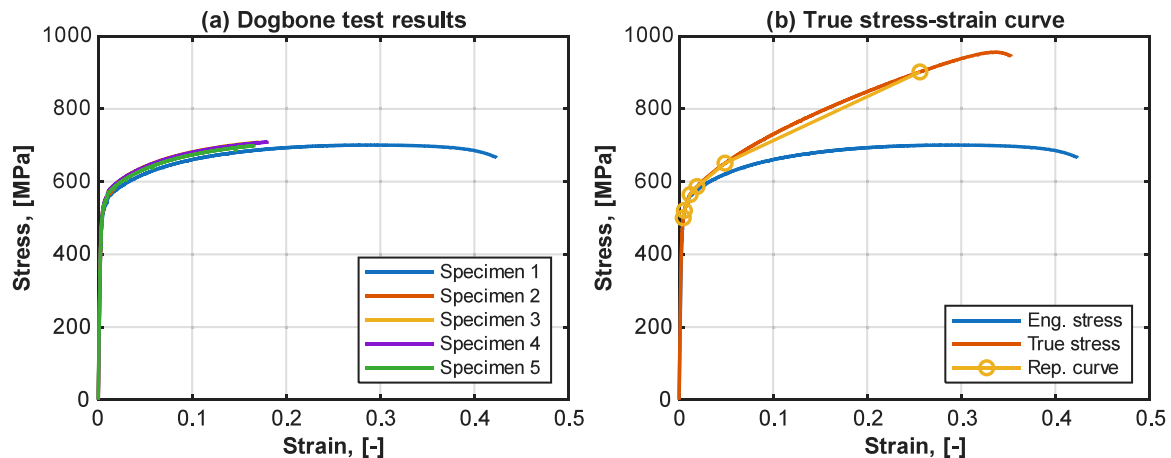


Fig. 3. Engineering and true stress-strain relations.

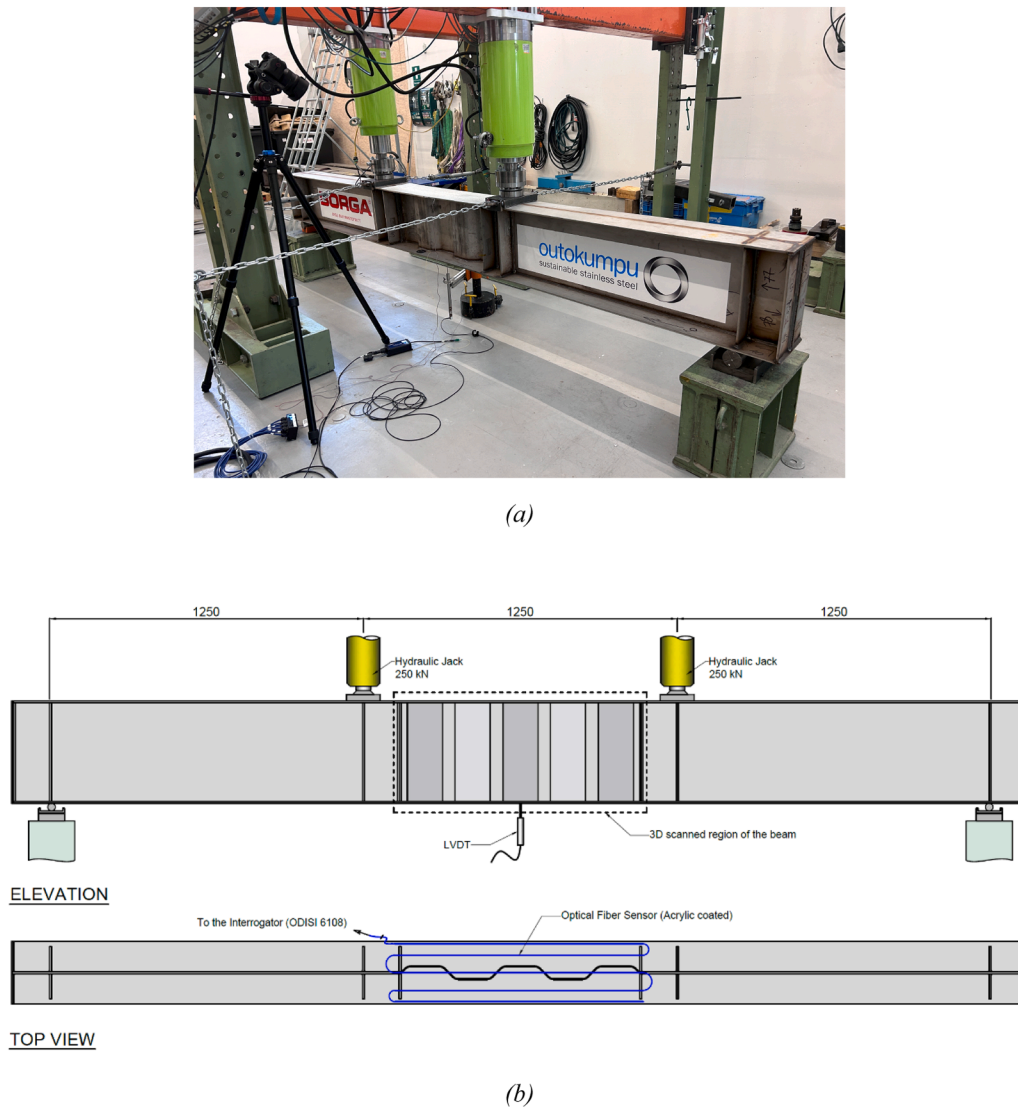


Fig. 4. (a) Chain connectors for the purpose of lateral stability; (b) Test set-up and instrumentation. Measurements in mm.

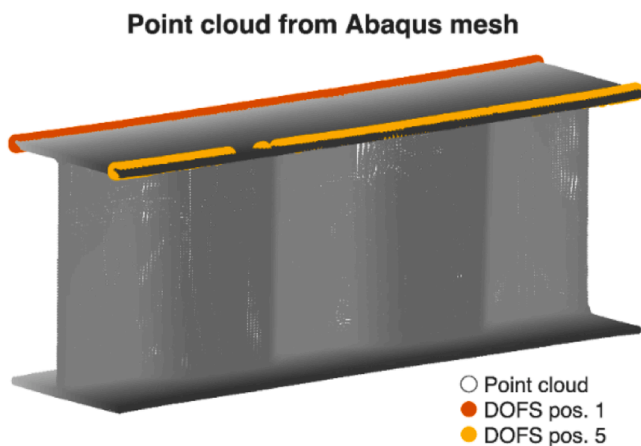


Fig. 5. Point cloud from 3D scanning and generated surface mesh.

Although further mesh refinement can reduce the number of distorted elements, the mesh-convergence assessment showed negligible changes in the predicted structural response. The adopted mesh was therefore

considered sufficiently accurate and numerically suitable for the specimen investigated.

To assess the accuracy of the transformation from the raw 3D scanning data to the final solid finite-element mesh, the vertical coordinates along two longitudinal lines on the top flange surface were compared. Specifically, the coordinates along the locations corresponding to DOFS Nos. 1 and 5, shown in Fig. 6, were extracted from both the processed point cloud and the finite-element mesh and compared, as presented in Fig. 7 (c and d).

Since only the central segment is scanned and imported into ABAQUS, and this segment is subjected to pure bending, the boundary conditions are defined such that both ends are free to rotate, with two equal end moments applied as the loading condition. It is worth noting that all nodes on each end section are kinematically coupled to the centroid of the respective section. Both the boundary conditions and the applied moments are imposed at these centroids, see Fig. 8. Furthermore, the deflection of this isolated segment differs from that of the same segment when it is part of the full beam. Therefore, since only the scanned central segment was modelled, the finite element deflections were adjusted by adding the theoretically estimated vertical deflection contribution of the remaining beam outside the scanned region. This enabled a direct comparison of the load–deflection curves with the experimental results for the complete test specimen.

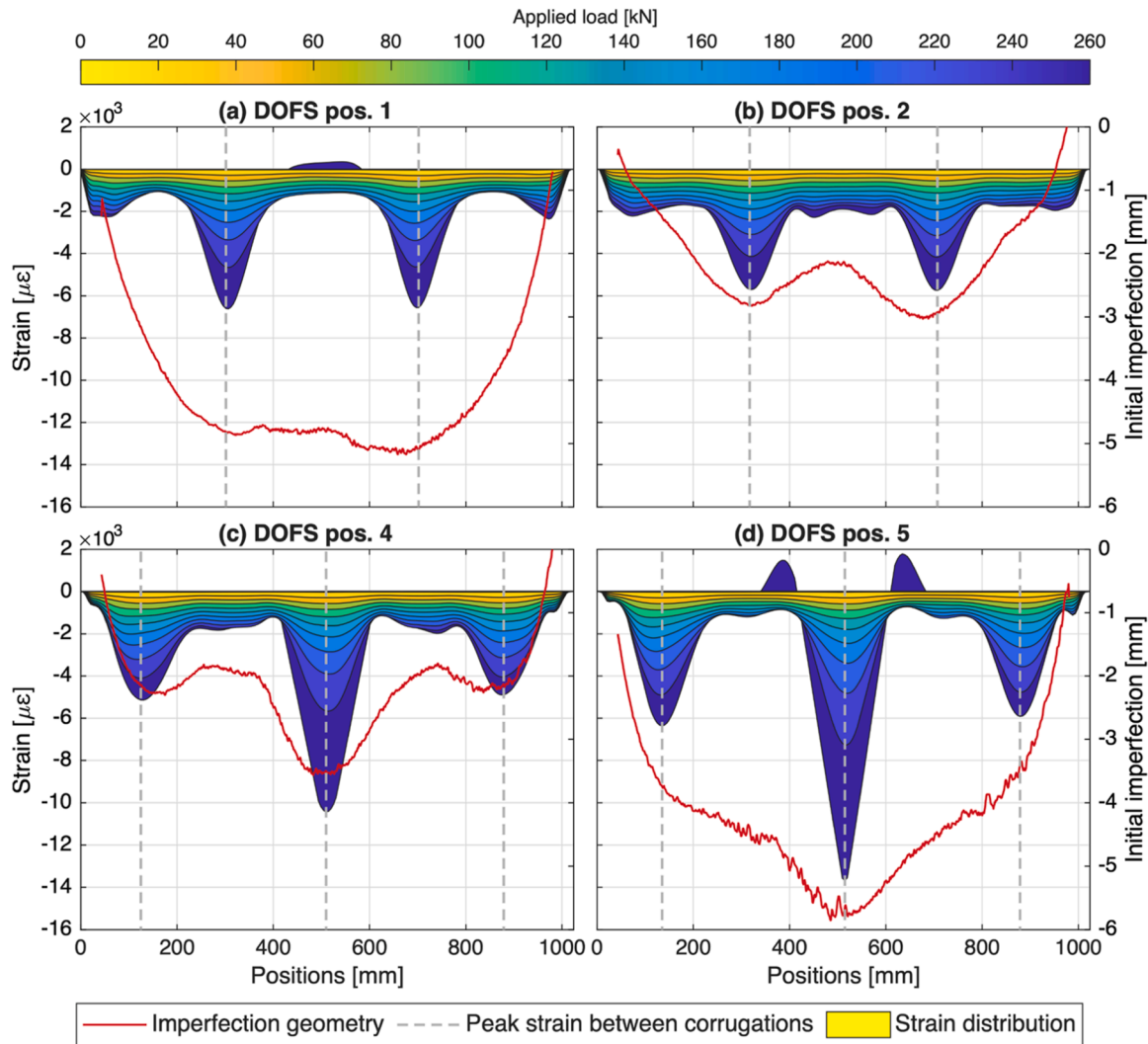


Fig. 6. Initial imperfections of the top flange.

2.3.2. Perfect geometry-based FEM including initial imperfection based on eigen mode shapes

In the next step of the finite element studies, the whole beam based on the nominal measurements given for production, see Fig. 1, was modelled with no initial imperfections. S4, a linear quadrilateral shell element with a size of 10 mm, was used to model the entire beam. Loads were introduced as point loads at corresponding positions and the supports were placed under the side stiffeners, allowing rotation but preventing vertical displacements. Further, one of the supports prevented the horizontal displacement as well, for model stability reasons, see Fig. 9. Following that, the eigen buckling modes were extracted using a linear buckling analysis. To evaluate the imperfection sensitivity, nonlinear analysis method is employed including the initial geometric imperfections based on the extracted buckling modes scaled to a specific imperfection value. Three different approaches to obtain such scaling factor were followed: (1) measured amplitude based on the 3D scanning geometry, (2) Eurocode-based amplitude, and (3) adjusted measured amplitude as derived in 2.3.3.

The scaling factor for the first approach can be obtained directly from Fig. 6. As it can be seen the maximum imperfection amplitude could be estimated to be around 6 mm, on the worse side of the beam, and that value was therefore used in FEM. However, for the second approach, according to EN 1993-1-5, no explicit imperfection model or recommended equivalent geometric imperfection amplitude is specified for the local buckling of flanges in corrugated web girders. For flat web

beams, however, it is commonly accepted, based on ECCS recommendations and the background calibration of the Eurocode provisions, that the maximum amplitude of out-of-plane imperfections in a compressed flange plate may be taken as $c_f/50$, where c_f denotes the flange outstand. In the absence of specific guidance for corrugated web girders, the same imperfection model is herein adopted for the flange plates. Accordingly, the imperfection amplitude is defined as $c_f/50$, based on the maximum flange outstand. In the present study, the maximum flange outstand is approximately 200 mm, which results in a corresponding initial imperfection amplitude of 4 mm.

Lastly, for the third approach, a refined measurement of the local imperfections of the flange plate (excluding the global initial deflections) was quantified based on the 3D scan, in accordance with the procedure outlined in the subsequent section.

2.3.3. Derived local initial imperfection from 3D scanner

A closer examination of Fig. 6 indicates that the flange exhibits two distinct types of initial imperfections with different characteristic length scales: (i) a long-length global deflection of the entire flange plate, constrained by the stiffeners at the end of the central segment, and (ii) an additional shorter-length local deflection occurring in the regions between the inclined parts of the corrugations, which effectively act as intermediate stiffeners. The purpose of the decomposition was to remove the long-wavelength global component while retaining the

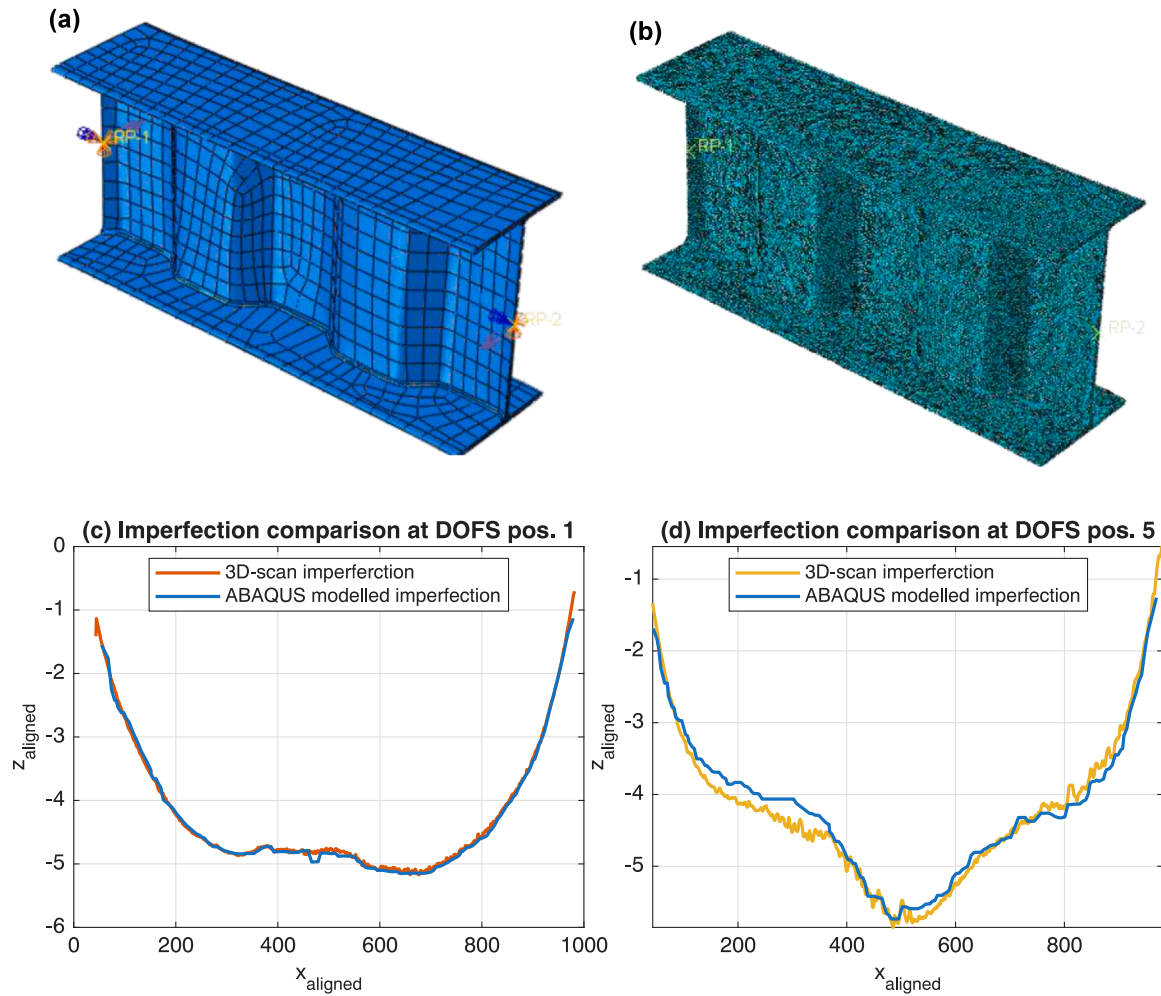


Fig. 7. (a) Solid geometry from 3D scanning; (b) Resulting mesh in ABAQUS. (c) and (d) Comparison between local imperfections measured from 3D scanning and FEM mesh at two different fiber positions.

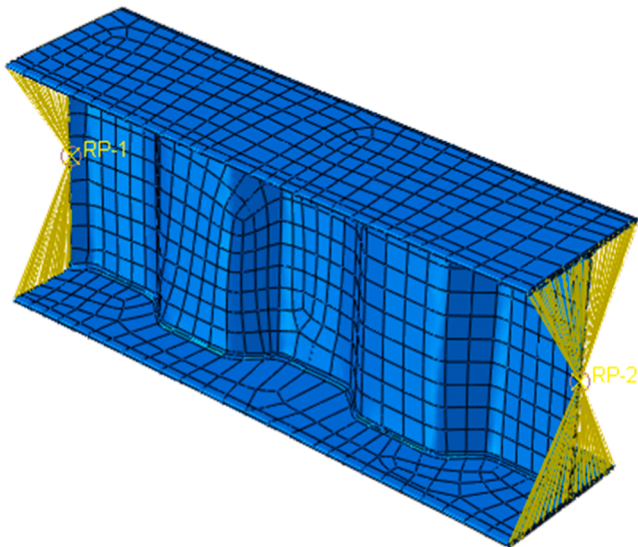


Fig. 8. Boundary conditions and loading of the beam.

corrugation-scale deviations relevant to local flange buckling. The resulting local imperfection can subsequently be used in the FEM as the imperfection amplitude scaling factor for the local buckling of the

flange. To this end, the vertical coordinates of the nodes located at DOFS positions 1 and 5, shown in Fig. 6, were analysed.

The decomposition followed a two-step procedure. First, a smooth reference profile representing the global flange deflection was fitted to the scanned coordinates. The fitted global profile was then subtracted from the measured coordinates, and the local imperfection was evaluated from the resulting residuals, as shown in Fig. 10. It should be noted that because the corrugation folds provide intermediate restraint to the flange, the shape of the global component depends on the number and arrangement of the corrugations on each side of the beam.

For the side featuring two corrugations, the global profile exhibits a single dominant curvature and therefore a second-degree parabolic function was fitted to estimate the global deflection, as illustrated in Fig. 10a. This choice is motivated because a quadratic polynomial is the lowest-order symmetric polynomial capable of representing a profile with a single extremum. In contrast, for the side with three corrugations, the central corrugation provides an additional intermediate restraint, producing a deformation profile with more than one extremum. Such a profile cannot be represented by a second-order polynomial, which contains only one extremum. A fourth-order polynomial was therefore adopted, see Fig. 10b, because it is the lowest-order symmetric polynomial capable of representing the observed multi-lobed deformation shape. The polynomial orders were thus selected based on the corrugation geometry and the corresponding expected global deformation pattern, rather than solely on the basis of curve-fitting accuracy. Higher-order polynomials were avoided because they could begin to reproduce

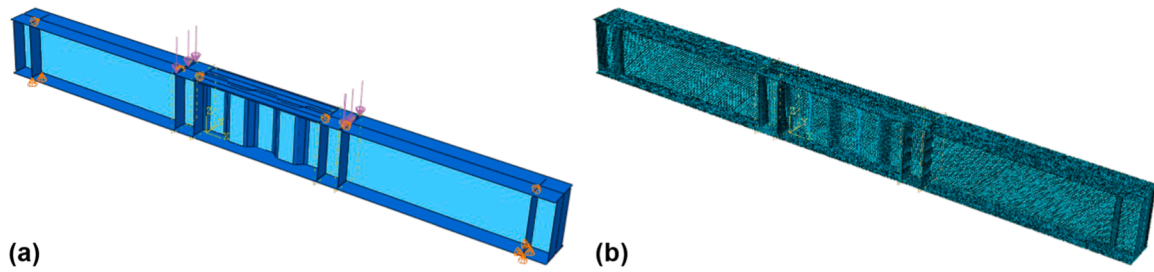


Fig. 9. (a) ABAQUS model with shell elements (b) mesh of the beam.

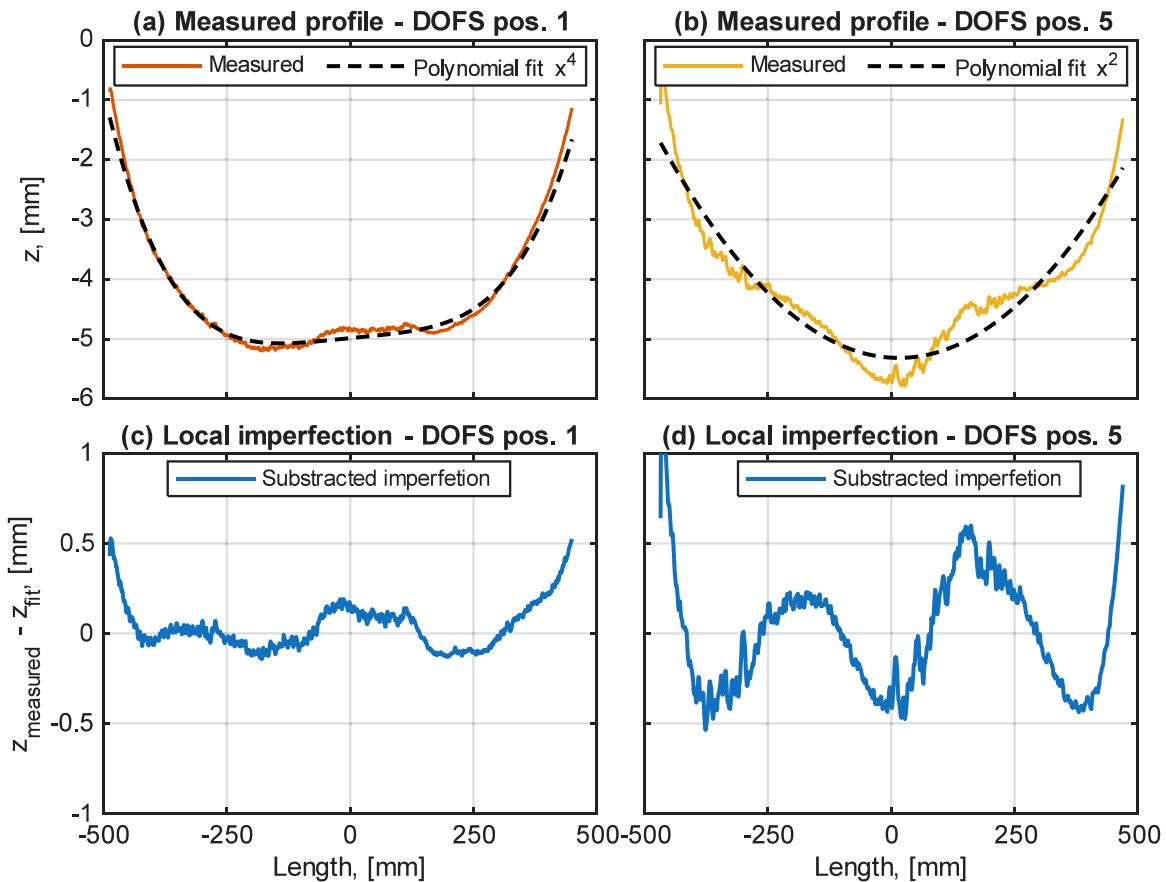


Fig. 10. Global and local imperfection from 3D scanner.

the shorter-wavelength variations that the decomposition was intended to retain as local imperfections.

By subtracting the approximated global imperfection from the scanned vertical coordinate data, an estimate of the local imperfection distribution was obtained, as illustrated in Fig. 10c and d. These figures show that the local imperfections fluctuate within a range of approximately ± 0.5 mm, corresponding to an initial imperfection amplitude of 1 mm.

3. Results and discussion

Experimental results in Fig. 11 presents the relationship between the applied load and the recorded mid-span displacement. The beam initially exhibited linear elastic behaviour up to approximately 150 kN. Beyond this point, a nonlinear response was observed, reflecting the characteristic material behaviour of stainless steel. The maximum load attained by the beam was 258 kN, at which failure occurred due to local buckling of the top flange, as shown in Fig. 11a, coupled with material

plasticity. Following the peak load, the load-carrying capacity gradually decreased until the test was terminated at a mid-span displacement of approximately 35 mm.

3.1. Strain results from DOFS

Fig. 12 illustrates the strain profiles at positions 1, 2, 4, and 5 of the DOFS, as described in Fig. 6(b), under incrementally increasing load levels. The measured strain values exhibit increasingly negative strains with increasing load, indicating progressive compression of the top flange. This behaviour is consistent with classical beam theory and the applied loading conditions. It is important to note that yielding initiates at approximately 150 kN ($\approx 2300 \mu\epsilon$), after which the compressive strains continue to increase, reaching values between 10000 and 14000 $\mu\epsilon$ at load levels of 240–258 kN. Upon reaching the ultimate load, the strain at certain locations between corrugations reverses sign, changing from compression to tension. This phenomenon is attributed to local buckling-induced out-of-plane deformations of the flange plate, which

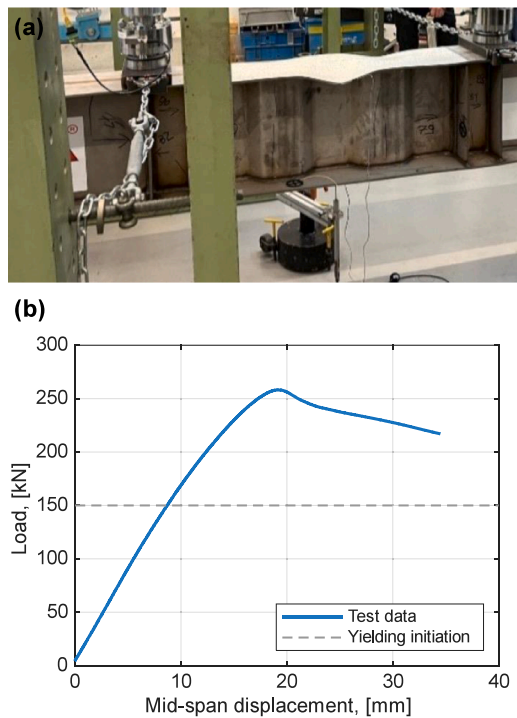


Fig. 11. (a) Flange local buckling after testing; (b) Load mid-span displacement curve.

alter the stress state such that the top surface of the flange experiences tensile strains instead of compressive strains.

Further analysis was conducted by comparing the measured strain responses with the vertical positional data extracted from the 3D-scanned geometry at the exact cable locations. The comparison reveals that the strain magnitudes recorded by the DOFS exhibit a degree of proportionality to the initial geometric imperfections of the beam, largely independent of the applied load level, as illustrated in Fig. 12 (a–d). Locations associated with greater initial out-of-plane deviations generally correspond to higher strain magnitudes throughout the loading history.

This observation suggests that the measured strain distribution is not governed solely by the externally applied loading but is also significantly influenced by the beam's original unloaded geometry. The presence of initial imperfections appears to affect the local stiffness and stress redistribution within the flange, thereby influencing the evolution of strain concentrations during loading. Such behavior is consistent with the known sensitivity of thin-walled steel members to geometric imperfections, particularly in regions susceptible to local instability and buckling phenomena. Consequently, the results indicate a strong interaction between the initial geometric profile of the beam and its subsequent structural response, highlighting the importance of accounting for as-built imperfections when interpreting distributed strain measurements and assessing structural behavior.

To facilitate a clearer visualization and interpretation of this relationship, an interpolated strain surface was generated based on the assumption of linear strain variation between adjacent sensor locations. The resulting strain field was subsequently compared with the flange geometry obtained from the 3D-scanned measurements, as presented in Fig. 13.

3.2. Numerical results

3.2.1. 3D scanned-based FEM

Fig. 14 presents a direct comparison between the experimentally measured load–midspan displacement response and the corresponding

results obtained from the finite element model developed in ABAQUS using the 3D-scanned beam geometry. Overall, an excellent correlation is observed between the experimental and numerical responses. The finite element model accurately captures both the ultimate load capacity and the corresponding displacement at peak load, demonstrating the capability of the modelling approach to reproduce the global structural behavior with a high degree of accuracy.

Minor discrepancies become apparent in the post-peak response following the onset of local buckling. In particular, the experimental results exhibit a slightly more abrupt reduction in load-carrying capacity compared with the numerical predictions. This behavior is primarily attributed to the practical difficulties associated with maintaining stable load and displacement control during the physical test after failure initiation, whereas the finite element model can follow the post-buckling response under idealized numerical conditions with greater stability and continuity.

3.2.2. Model validation through DOFS

To further validate the 3D scan-based finite element model, Fig. 15 presents a comparison between the strains measured by the DOFS and FEM predicted values at the corresponding sensor locations under the ultimate load level of 258 kN. The comparison demonstrates, once again, a strong correlation between experimental measurements and numerical predictions. The close agreement between the measured and simulated strain responses further confirms the reliability of incorporating detailed 3D-scanned geometric data into the numerical modelling framework. These findings demonstrate that the proposed modelling approach is not only effective in predicting the global load–displacement behavior, but also capable of accurately reproducing local strain fields and instability mechanisms.

A detailed comparison between the strain fields obtained from the FE model and those measured by the DOFS on the top flange is presented in Fig. 16. For the DOFS data, continuous strain fields were derived through linear interpolation between the cable measurement locations to enable direct spatial comparison with the finite element results.

The comparison demonstrates a high degree of consistency between the two approaches, particularly with respect to the location and evolution of the strain concentrations. Regions exhibiting elevated compressive and tensile strains are similarly identified in both the FEM and DOFS results, indicating that the numerical model successfully captures the influence of the beam's initial imperfections and accurately reproduces the structural response throughout the loading process. The close correspondence between the measured and simulated strain distributions further confirms the validity of incorporating the 3D-scanned geometry into the finite element modelling framework.

As illustrated in Fig. 16(d), following the onset of local flange buckling, portions of the strain field undergo a sign reversal, changing from negative strains associated with compressive bending stresses to positive tensile strains. This behavior indicates the formation of local buckling folds between the corrugations and the initiation of flange failure. The out-of-plane deformation of the flange locally alters the stress state, resulting in tensile strains developing on the top surface of the flange despite the overall compressive bending condition. Notably, these localized positive strain regions are accurately captured by both the FEM and the DOFS measurements, demonstrating the capability of the proposed experimental and numerical approaches to identify and characterize complex post-buckling deformation mechanisms.

3.2.3. FEM including idealized imperfections

As discussed in the introduction, a common finite element modelling approach for the design of thin-walled steel structures involves the use of shell-element models based on idealized geometry. Typically, a linear eigenvalue buckling analysis is first performed to determine the characteristic buckling modes of the member. The resulting eigenmodes are then introduced into the model as geometric imperfections through the application of an assumed imperfection amplitude scaling factor.

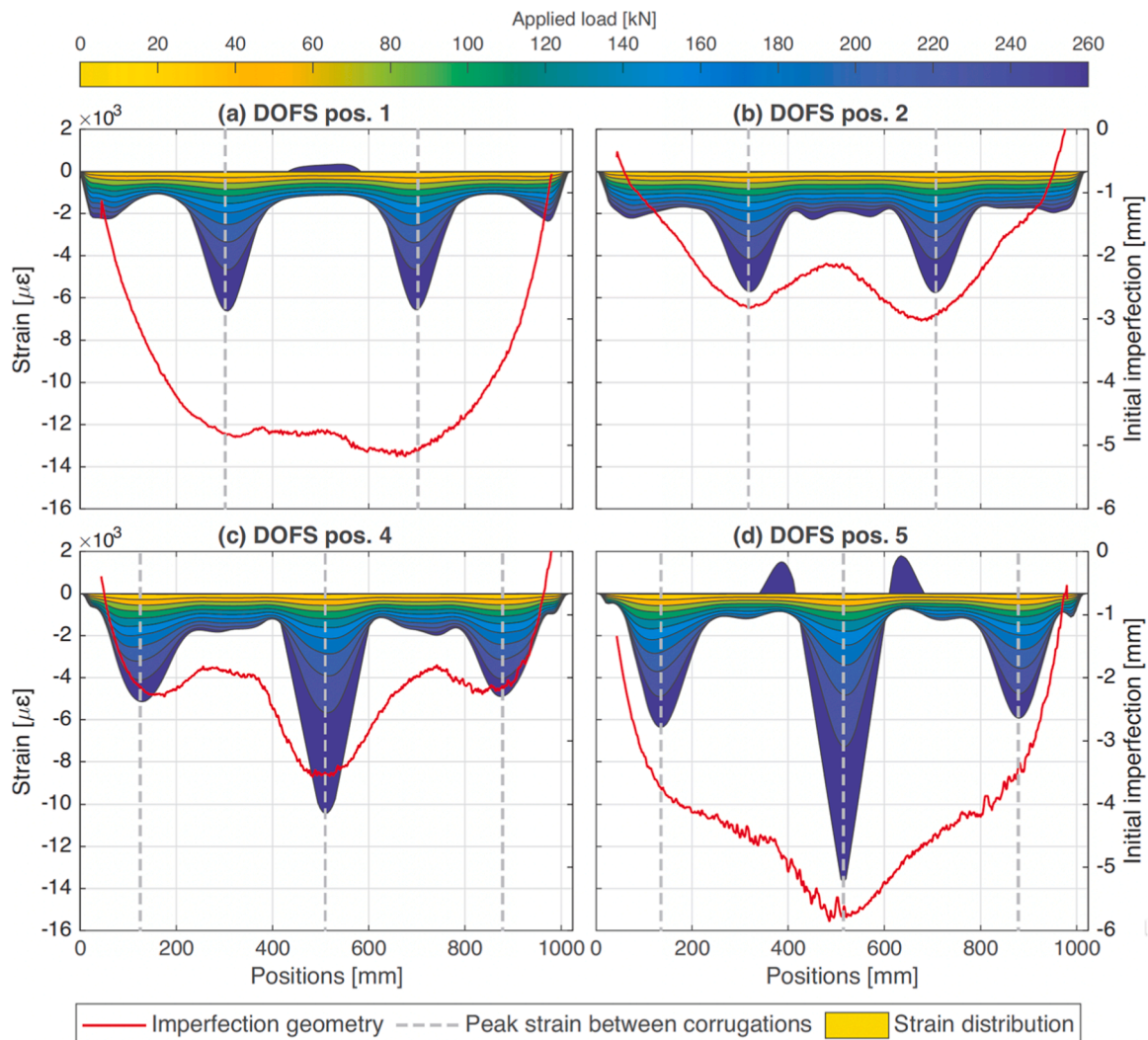


Fig. 12. Strain profiles from different DOFS positions and initial imperfection at the same location.

Subsequently, a geometrically nonlinear analysis incorporating these initial imperfections is conducted to evaluate the structural response and ultimate capacity. This methodology is widely adopted in both research and engineering practice due to its computational efficiency and ability to represent imperfection-sensitive buckling behavior. Subsequently the first three first eigenmodes from the analysis carried out on the perfect geometry of the studied beam, are presented in Fig. 17. These modes illustrate the characteristic local buckling shapes associated with the corrugation geometry and provide the basis for introducing geometric imperfections into the subsequent nonlinear analyses.

When comparing the buckling mode shapes shown in Fig. 17 with the numerical and experimental results summarised in Fig. 16, it becomes clear that mode 1 with a negative amplification factor (i.e. reversed out-of-plane buckling deformation) most closely matches the observed failure mode and the strain patterns recorded by DOFS prior to failure. However, mode 3 with a positive amplification factor also provides a reasonable approximation on one side of the specimen, where flange failure occurred. Therefore, modes 1 and 3 were selected as initial imperfections and considered together with the three presented magnitude estimations discussed in Sections 2.3.2 and 2.3.3, namely: 3D-scan-based (6 mm), EC-based (4 mm), and adjusted values derived from the 3D scan measurements (1 mm).

Fig. 18 (a–d) presents a comparison of the load–mid-span displacement curves obtained from various numerical models, including the 3D-scanned-based model and the idealized model employing different

eigenmodes as initial imperfections, together with the experimental results. As shown in Fig. 18 (b and c), the idealized model consistently underestimates the beam capacity when the imperfection magnitude is taken directly from the 3D scan (6 mm) or from code recommendations (4 mm), regardless of the selected mode shape. However, when the global imperfection is removed from the 3D scan and only the local flange imperfection is used, a strong agreement between the experimental and numerical results is achieved for both Mode 1 and Mode 3 analyses. This indicates that local imperfections govern the failure behavior and therefore should be evaluated with greater accuracy.

Furthermore, Fig. 18(d) presents an analysis using Mode 1 in which the out-of-plane deflection occurs in the opposite direction to that observed experimentally by applying a positive imperfection amplitude of +1 mm. This results in a higher predicted capacity than both the corresponding analysis using a negative imperfection amplitude of −1 mm and the experimental results. It should be noted that, owing to the asymmetric geometry of the corrugated web, the flange local buckling behaviour is inherently asymmetric with respect to the web. In the tested specimen, the flange side with the larger outstand at the midspan buckled downward, as observed experimentally. However, the eigenvalue buckling analysis may predict the same mode shape with either upward or downward out-of-plane deflection. Therefore, when the predicted mode deflects opposite to the experimentally observed direction, the imperfection is multiplied by −1 to reproduce the physically relevant deformation. This sign reversal changes only the out-of-

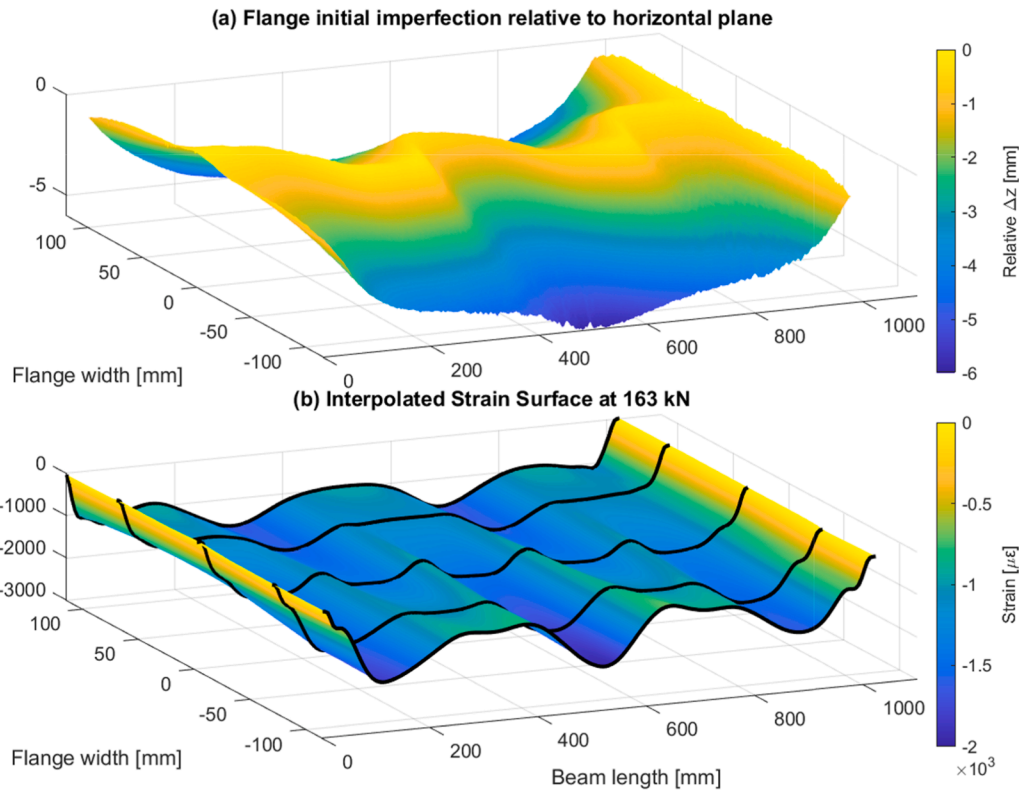


Fig. 13. (a) Initial imperfections of the top surface of the top flange measured from 3D scanner; (b) Interpolated strains from DOFS sensors. The black lines indicate the position of the DOFS along the beam's top flange.

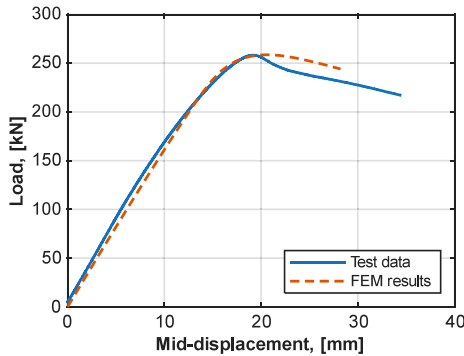


Fig. 14. Comparison between 3D FEM model and experimental results. Load vs mid-span displacement curves.

plane deflection direction of the same eigenmode and does not alter the asymmetric relationship between the flange and the corrugated web. The results therefore highlight that the first eigen buckling mode should not always be assumed to be critical solely based on its eigenvalue. Instead, for each eigenmode, both out-of-plane deflection directions should be evaluated, and the more critical response should be adopted. This is particularly important when a refined imperfection amplitude of **1 mm** is considered, since increasing the imperfection amplitude to **4 mm** or **6 mm** leads to lower predictions of the ultimate capacity due to the assumption of unrealistically large local imperfections, which in practice also incorporate global deflections.

3.3. Evaluation of local imperfections through DOFS

As seen in the previous section, accurately estimating beam capacity relies on properly evaluating initial imperfections. While 3D scanning

can provide precise geometric details leading to highly accurate numerical analyses, frequent use of such scans is impractical due to their complexity and the tedious nature of processing point clouds. Further important measurement errors can be introduced as the element size grows, due to the nature of such technologies. Alternatively, using simplified geometries makes the value assigned to initial imperfection a critical factor, being the magnitude of the local imperfection the one that yields more accurate estimations of the beam capacity. Again, estimating this local imperfection from the 3D scan is possible however, requires access to the flange surface and assumptions on the shape for the global imperfection to be able to isolate the local imperfection are needed.

To overcome these limitations, this paper proposes an alternative approach utilizing DOFS data to calculate imperfection values appropriate for simplified models. As demonstrated in Fig. 12, a clear correlation between strain and shape is observed across all load steps; consequently, a novel method is introduced for back-calculating local imperfections from the DOFS data.

4. Analytical derivation of the local imperfection estimation method

At low to moderate loading levels, the longitudinal stress acting on the top flange of the beam can be idealized as consisting of two components. The first arises from the global bending moment, while the second results from local bending associated with the initial geometric imperfections, as illustrated in Fig. 19. In deriving the subsequent calculation model, the contribution of the corrugated web to the longitudinal stiffness associated with global bending is neglected, consistent with the accordion behaviour of corrugated web girders. This assumption does not imply that the influence of the corrugated-web geometry on the flange response is neglected. The corrugation provides spatially varying restraint to the flange and can influence

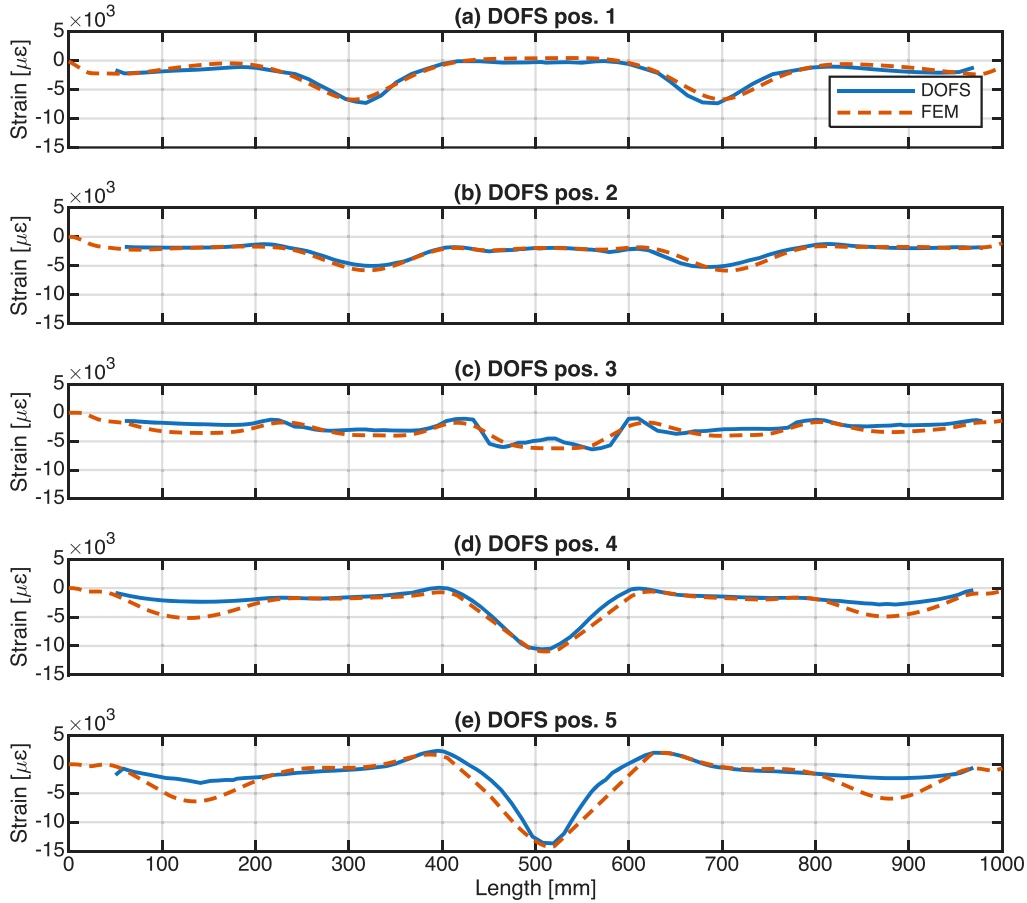


Fig. 15. Strain comparison between DOFS measurements and FEM model at DOFS locations for 258 kN.

transverse flange bending and the local stress and strain distributions. These three-dimensional effects are inherently present in the tested girder and are explicitly represented in the scan-based finite-element model through the measured geometry. However, they are not explicitly resolved in the simplified analytical derivation. The proposed analytical relationship should therefore be regarded as an elastic-range imperfection-estimation procedure, intended to estimate the initial flange imperfection from longitudinal strain measurements, and is not intended to predict plasticization or post-yield behaviour.

Due to the global bending moment, the top flange of the beam is subjected to a linear stress variation where the maximum stress in the topmost fibre, σ_f , can be calculated as:

$$\sigma_f = \frac{M_g}{I_b} \frac{h}{2} \quad (3)$$

where M_g is the global moment, h is the beam height and I_b is the moment of inertia of the beam, which can be approximated, neglecting the contribution of the web, as:

$$I_b = 2 \left[\frac{1}{12} b t^3 + b t \left(\frac{h-t}{2} \right)^2 \right] \quad (4)$$

where b and t are the width and thickness of the flanges, respectively. The stress increment on the top flange, $\Delta\sigma_f$, from the local bending moment, M_L , can be estimated from the axial force on the top flange, N_f , as:

$$\Delta\sigma_f = \frac{M_L}{I_f} \frac{t}{2} = \frac{N_f e_f}{I_f} \frac{t}{2} = \frac{\sigma_m \cdot b_{ef} t \cdot e_f}{\frac{1}{12} b_{ef} t^3} \frac{t}{2} = \frac{6\sigma_m e_f}{t} \quad (5)$$

where e_f is the magnitude of the local initial imperfection on the top flange, I_f is the moment of inertia of the top flange considering an effective width, b_{ef} , and σ_m , is the average stress on the top flange, defined as:

$$\sigma_m = \frac{M_g}{I_b} \left(\frac{h-t}{2} \right) \quad (6)$$

The stress increment on the top flange can be also estimated as the difference between the total stress and the maximum stress due to the global moment. Considering that for small load levels the steel is in the elastic range and that the strain measured with the optical fibre sensors, ϵ_{DOFS} , is the total strain on the surface of the top flange, we can write that:

$$\Delta\sigma_f = \sigma_{TOT} - \sigma_f = \epsilon_{DOFS} E_s - \sigma_f \quad (7)$$

Combining Eqs. (5) and (7) and rewriting σ_f as $\sigma_m \cdot \left(\frac{h}{h-t} \right)$, we finally get to the following expression:

$$e_f = \frac{t}{6} \left(\frac{\epsilon_{DOFS}}{\epsilon_m} - \frac{h}{h-t} \right) \quad (8)$$

which can be used to estimate the initial local imperfection of the top flange from strain measurements and the geometric parameters of the beam, provided that the loading conditions (i.e., the global bending moment) are known and the strain measurements are performed before yielding initiates.

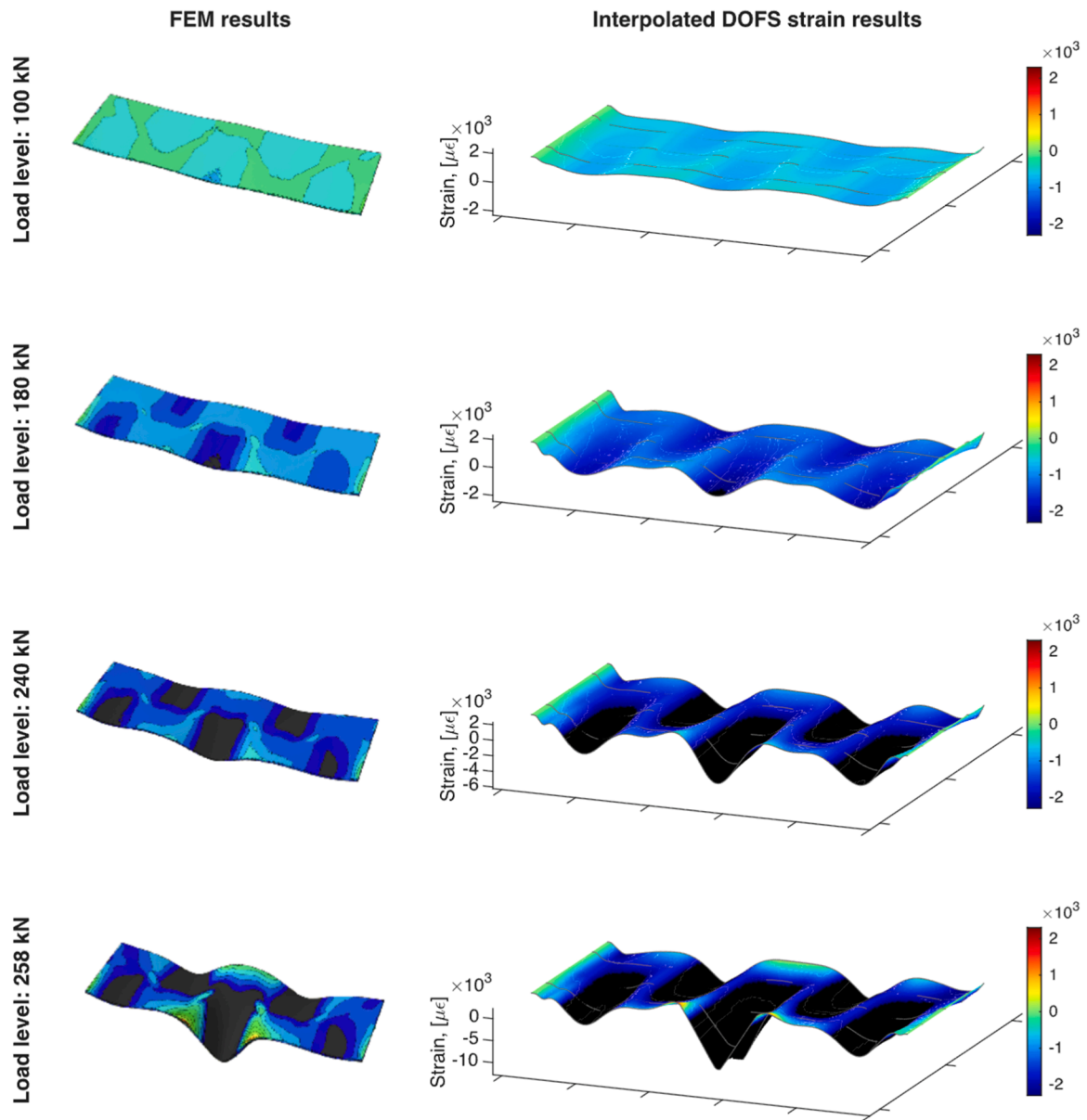


Fig. 16. Deformed shape and strain distribution on the top flange for different load steps.

4.1. Calculated initial imperfection

Fig. 20(a–d) compares local initial imperfections calculated using the method detailed in Section 4 with those derived from Section 2.3.3. The results indicate a strong overall correlation, particularly for DOFS position 5, which displays the closest match. Conversely, the sensor at position 1 demonstrates less agreement, reflecting smaller imperfections. This is explained due to an additional corrugation at that side, which reduces the imperfection's size and makes accurate measurement more challenging. However, a more detailed analysis by examining the shapes of the normalized imperfection values relative to their maximum, the alignment between calculated and measured shapes improves notably, as illustrated in Fig. 20 (c and d), indicating that the method and DOFS strains is suitable, and strengthening the relation between measured strains and imperfections. Notably, sensor position 5 is critical, as it determines failure and thus holds particular importance in this analysis. These results suggest that DOFS strain measurements are highly effective for detecting such imperfections. Moreover, the initial imperfection values obtained through this approach have been validated

for load levels up to 100 kN, confirming its reliability for service conditions.

5. Conclusions

This paper examines various approaches for evaluating the ultimate capacity of a stainless-steel corrugated web girder specimen subjected to flange local buckling. The girder was fabricated and tested to failure using a symmetric four-point bending configuration. Prior to testing, the midsection of the beam was 3D scanned to accurately quantify initial imperfections due to manufacturing processes. To assess the beam's ultimate capacity, several numerical modelling techniques were employed using the commercial finite element software ABAQUS. The accuracy of the predicted ultimate load depended significantly on the initial imperfections. Consequently, multiple approaches for estimating these imperfections were explored and validated. The following conclusions are drawn from this study:

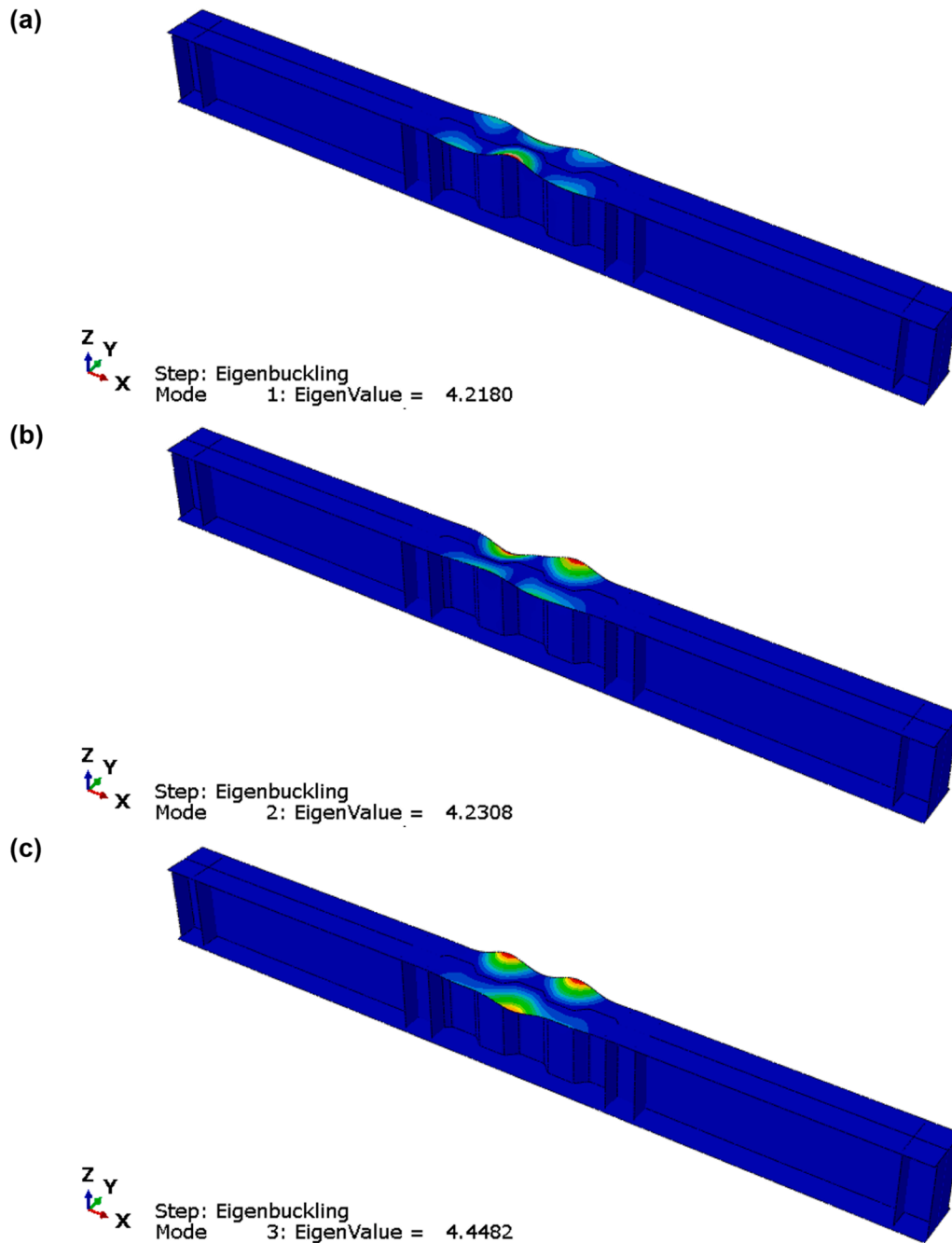


Fig. 17. Calculated eigen modes of the beam: (a) Mode 1; (b) mode 2 and (c) mode 3.

- The 3D scanning technique provided an accurate representation of the beam geometry, proving suitable for numerical modelling and evaluation of the structural response in comparison with the experimental results, even when simplified models considering only the central portion of the beam were employed.
- When the beam was modelled using an idealized geometry without explicit imperfections, and the initial imperfection was introduced through eigenmode analysis using amplitudes derived from global 3D scanner measurements, the predicted ultimate capacity was significantly underestimated.
- A similarly significant underestimation of the ultimate capacity was also obtained when the initial imperfection amplitude was determined according to EC-based recommendations.
- The results further highlight that the first eigenbuckling mode should not always be assumed to represent the critical mode. The direction

(sign) of the introduced imperfection was shown to have a significant influence on the structural response. Therefore, for each eigenmode shape, both imperfection directions should be evaluated, and the most critical response should be adopted. This is particularly important when a refined imperfection magnitude of 1 mm is considered, since increasing the imperfection magnitude to 4 mm or 6 mm would lead to lower predictions of the ultimate capacity due to the assumption of unrealistically large local imperfections, which in practice also include global deflections.

- These findings indicate that the flange buckling behavior is strongly influenced by local imperfections associated with web corrugation. This was demonstrated using the same simplified numerical model while introducing local imperfections extracted from the 3D-scanned geometry after removal of the global imperfections.

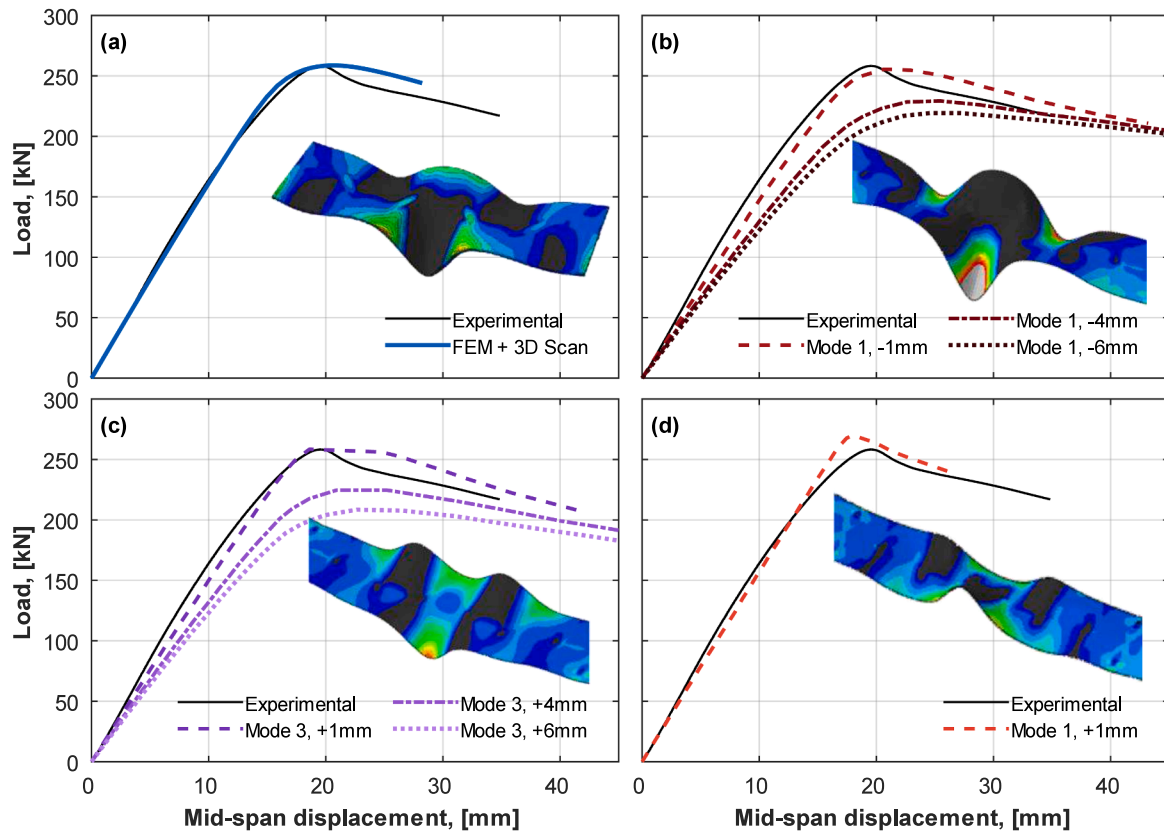


Fig. 18. Load vs mid-span displacement curve comparison for experimental and numerical analyses: (a) Geometry based on 3D scanner; (b) Ideal model with 1st buckling mode imperfection for different negative initial imperfection values; (c) Ideal model with 3rd buckling mode imperfection for different negative initial imperfection values; (d) Ideal model with 1st buckling mode imperfection for a positive initial imperfection value of 1 mm.

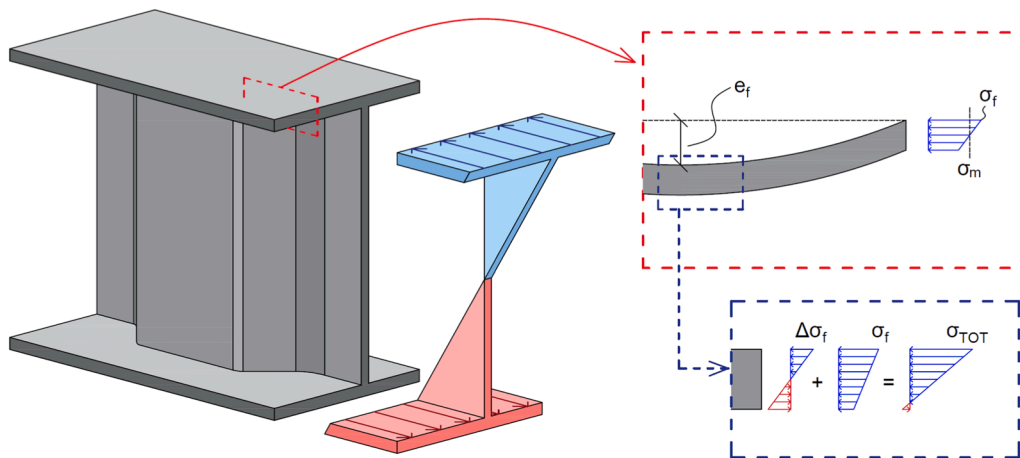


Fig. 19. Schematic representation of the stress distribution on the top flange of an I-girder including the contributions of global and local bending moments.

- The DOFS measurements accurately captured the longitudinal strain distribution along the beam axis and showed strong agreement with the FEM results.
- The results further demonstrate that the strain profiles are closely related to the initial imperfections, enabling back-calculation of the original beam geometry. Based on this observation, a method for extracting local imperfections from DOFS measurements was proposed and validated as a simpler alternative to full 3D scanning.
- The proposed method for determining initial local imperfections from strain measurements showed very good agreement with the 3D scanning results for service load levels ranging from a few

kilonewtons up to 100 kN, demonstrating its potential as input for the simplified modelling approach. However, this conclusion is based on the investigated specimen and should be further verified through additional experimental and numerical studies covering a wider range of corrugated web girder configurations.

CRediT authorship contribution statement

Mozhdeh Amani: Writing – original draft, Visualization, Validation, Software, Investigation, Formal analysis. **Ignasi Fernandez:** Writing – original draft, Visualization, Validation, Software, Methodology, Data

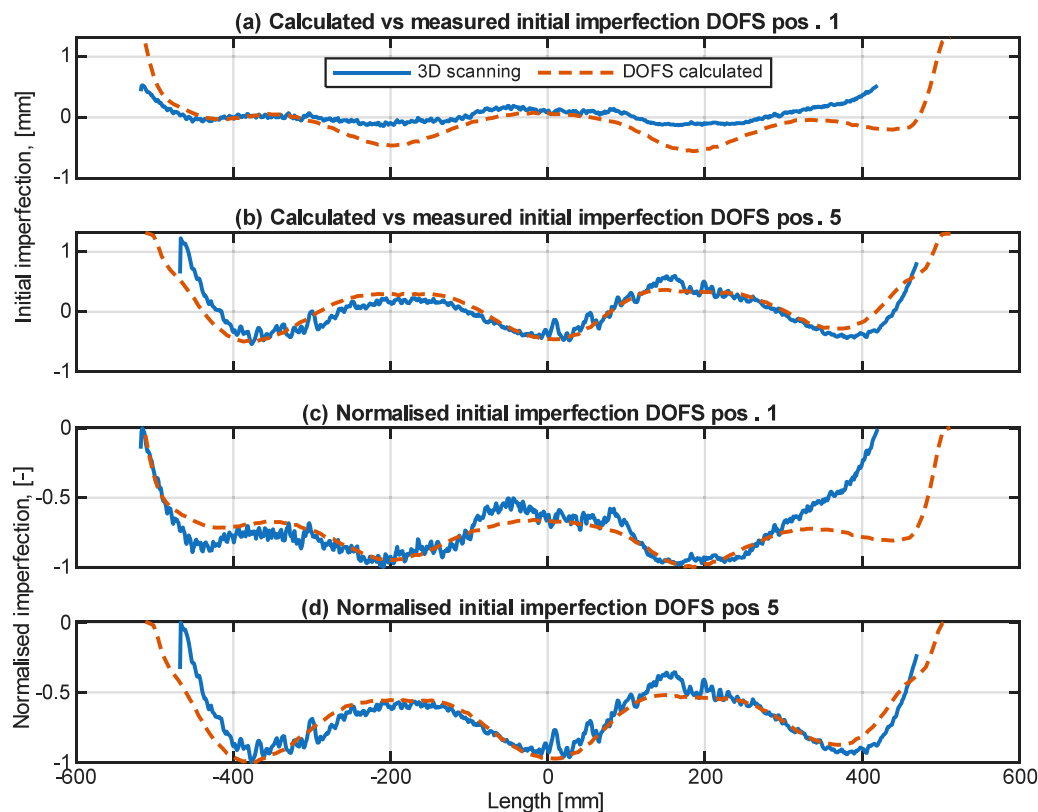


Fig. 20. Comparison between calculated initial imperfection from 3D scanned geometry and DOFS sensors.

curation. **Carlos G. Berrocal:** Writing – review & editing, Visualization, Validation, Software, Methodology, Investigation, Data curation. **Fatima Hlal:** Conceptualization, Data curation, Writing – review & editing. **Mohammad Al-Emrani:** Conceptualization, Funding acquisition, Methodology, Project administration, Resources, Supervision, Writing – review & editing.

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.

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Data availability

Data will be made available on request.

References

- [1] B. Jäger, L. Dunai, B. Kövesdi, Flange buckling behavior of girders with corrugated web part I: experimental study, *Thin Walled Struct.* 118 (2017) 181–195, <https://doi.org/10.1016/j.tws.2017.05.021>.
- [2] I. Baláz, Y. Koleková, Influence of transverse bending moment in the flange of corrugated I-girders, *Procedia Eng.* 40 (2012) 26–31, <https://doi.org/10.1016/j.proeng.2012.07.050>.
- [3] H.H. Abbas, R. Sause, R.G. Driver, Analysis of flange transverse bending of corrugated web I-girders under In-plane loads, *J. Struct. Eng.* 133 (2007) 347–355, [https://doi.org/10.1061/\(ASCE\)0733-9445\(2007\)133:3\(347\)](https://doi.org/10.1061/(ASCE)0733-9445(2007)133:3(347)).
- [4] B. Jäger, L. Dunai, B. Kövesdi, Flange buckling behavior of girders with corrugated web part I: experimental study, *Thin Walled Struct.* 118 (2017) 181–195, <https://doi.org/10.1016/j.tws.2017.05.021>.
- [5] A.A. Elkawas, M.F. Hassanein, A.M. El Hadidy, M.H. El-Boghdadi, M. Elchalakani, Behaviour of corrugated web girders subjected to lateral-torsional buckling: experimental tests and numerical modelling, *Structures* 33 (2021) 152–168, <https://doi.org/10.1016/J.ISTRUC.2021.04.057>.
- [6] X. Deng, I.A. Sharaky, Y.M. Alharthi, A.S. Elamary, Effect of Shear and pure bending spans on the behaviour of steel beams with corrugated webs, *Materials* 15 (2022) 4675, <https://doi.org/10.3390/MA15134675>. Page 4675 15.
- [7] B. Jäger, L. Dunai, B. Kövesdi, Flange buckling behavior of girders with corrugated web part II: numerical study and design method development, *Thin Walled Struct.* 118 (2017) 238–252, <https://doi.org/10.1016/j.tws.2017.05.020>.
- [8] M.F. Hassanein, A.A. Elkawas, M. Bock, Y.B. Shao, M. Elchalakani, Effect of using slender flanges on EN 1993-1-5 design model of mono-symmetric S460 corrugated web bridge girders, *Structures* 33 (2021) 330–342, <https://doi.org/10.1016/J.ISTRUC.2021.04.066>.
- [9] F. Hlal, M. Al-Emrani, Flange buckling in stainless-steel corrugated webs I-girders under pure bending: numerical study, *J. Constr. Steel Res.* 208 (2023) 108031, <https://doi.org/10.1016/J.JCSR.2023.108031>.
- [10] Q. Inaam, A. Upadhyay, Accordion effect in bridge girders with corrugated webs, *J. Constr. Steel Res.* 188 (2022) 107040, <https://doi.org/10.1016/J.JCSR.2021.107040>.
- [11] S.A. Kumar, F.A. Sofi, J.A. Bhat, M. Zhou, Comprehensive analysis of corrugated-web girders: a systematic review, *Thin Walled Struct.* 208 (2025) 112636, <https://doi.org/10.1016/J.TWS.2024.112636>.
- [12] M.F. Hassanein, A.A. Elkawas, M. Bock, M. Elchalakani, Y.B. Shao, Lateral-torsional buckling strength of corrugated web bridge girders: EC3 and AISC modified design methods, *Thin Walled Struct.* 176 (2022) 109373, <https://doi.org/10.1016/J.TWS.2022.109373>.
- [13] D. Kollár, B. Kövesdi, Welding simulation of corrugated web girders - part 1: effect of manufacturing on residual stresses and imperfections, *Thin Walled Struct.* 146 (2020) 106107, <https://doi.org/10.1016/J.TWS.2019.04.006>.
- [14] S. Mikhailov, J. van Wittenberghe, G. Luyckx, P. Thibaut, T. Geernaert, F. Berghmans, Fatigue weld crack detection using distributed fiber optic strain sensing, *Int. J. Fatigue* 176 (2023) 107851, <https://doi.org/10.1016/J.IJFATIGUE.2023.107851>.
- [15] J. Gillham, E.C. Bentz, N.A. Hoult, Measuring support reactions and damage detection in steel beams using distributed strain sensing, *Eng. Struct.* 262 (2022) 114389, <https://doi.org/10.1016/J.ENGSTRUCT.2022.114389>.

- [16] H. Koh, N. opperman, G. Arulananthan, T. Sputo, J.C. Hampton, H.B. Blum, Buckling detection of profiled steel deck using strain measurement techniques in a new vacuum box apparatus, 2024. <https://ssirl.cee.wisc.edu/holistic-steel-systems/>.
- [17] B. De Pauw, M. Hinderdael, M. Moonens, D. De Baere, T. Geernaert, F. Berghmans, P. Guillaume, Fatigue failure monitoring of 316L stainless steel coupons using optical fibre based distributed strain sensing, *Smart Mater. Struct.* 28 (2019) 105054, <https://doi.org/10.1088/1361-665X/AB32CD>.
- [18] M.F. Bado, J.R. Casas, A. Barrias, Performance of rayleigh-based distributed optical fiber sensors bonded to reinforcing bars in bending, *Sensors (Switzerland)* 18 (2018), <https://doi.org/10.3390/s18093125>.
- [19] I. Fernandez, J.M. Bairán, A.R. Marí, 3D FEM model development from 3D optical measurement technique applied to corroded steel bars, *Constr. Build. Mater.* 124 (2016) 519–532, <https://doi.org/10.1016/j.conbuildmat.2016.07.133>.