



Closed-form model for ultimate capacity of bolted T-stubs within the Eurocode framework

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ABSTRACT

Bolted T-stubs govern the flexural response of steel beam-to-column joints. Eurocode 3 Part 1–8 provides reliable formulations for stiffness and resistance, but does not address ultimate resistance and ductility, which are crucial for performance-based assessment. Advanced models capture the full response but are computationally demanding, while closed-form approaches have limited ability to represent failure mode interaction. This paper proposes a simplified closed-form analytical model to predict both ultimate resistance and deformation capacity of bolted T-stubs. The formulation is based on an improved classification of failure modes and introduces two main enhancements: a mechanically consistent evaluation of the resultant contact force position and the inclusion of transition mechanisms between flange-dominated and bolt-dominated behaviour. In addition to classical failure modes, two intermediate mechanisms (MEC-1.2 and MEC-2.3) are introduced to better represent the progressive evolution of collapse. Closed-form expressions are derived by accounting for flange plasticity, bolt elongation, and strain-hardening effects. The model is first verified against an advanced mechanical model through a parametric study on 138 cases, showing strong agreement, including in the identification of governing failure modes. It is then validated against 58 experimental tests, providing accurate predictions of both ultimate resistance and deformation capacity, with mean values of $F_u/F_{u,exp} = 1.00$ and $\delta_u/\delta_{u,exp} = 0.94$, and coefficients of variation equal to 0.12 and 0.19, respectively. The proposed model ensures a superior balance between consistency, accuracy, and simplicity, providing a solid basis for design applications and future EN 1993-1-8 developments.

1. Introduction

Beam-to-column joints play a critical role in the overall performance of steel Moment-Resisting Frames (MRFs), as they ensure structural continuity and significantly influence the response in terms of both stiffness and strength. To determine joints key response parameters, Eurocode 3 Part 1–8 [1] provides a well-established design framework based on the component method, whereby a joint is idealised as an assembly of discrete mechanical components whose combined response reproduces the global joint behaviour. While this approach has proven effective in predicting joint stiffness and plastic resistance and is widely adopted in routine design practice, its applicability remains limited as the component method does not explicitly address the prediction of ultimate resistance and deformation capacity. This limitation becomes

particularly relevant when connections are weaker than the connected beam (i.e. partial-strength joints), as in such cases the available plastic reserves may be mobilised under specific loading conditions (e.g. seismic, robustness or other exceptional loading scenarios). In such cases, a rational and reliable framework is required for the explicit assessment of joint deformation capacity. Within a performance-based perspective, a comparison between the deformation capacity and demand is essential to ensure that the joint can sustain the imposed actions without incurring premature or brittle failure, thereby guaranteeing an adequate performance under the prescribed loading conditions (Fig. 1).

In order to fill this knowledge gap, over the past decades significant research effort has been devoted to the development of mechanical models able to assess the ultimate capacity of bolted T-stubs and numerous simplified mechanical models have been proposed since the

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1960s by various researchers. A review of the existing literature shows that these models can generally be grouped into three main categories: those aimed at predicting resistance, those focused on initial stiffness, and those intended to reproduce the complete force–displacement response of T-stubs up to failure.

Early efforts to characterize the mechanical behaviour of T-stubs were carried out by Zoetemeijer [2], Douty and McGuire [3], and Yee and Melchers [4]. In their pioneering study, Douty and McGuire [3] examined the influence of material strain hardening on T-stub resistance and introduced a simplified strength model based on the ultimate strength of the flange material rather than its yield stress. Later, Zoetemeijer [2] investigated the impact of three-dimensional effects on T-stub resistance. By adopting a yield-line approach, he derived expressions for equivalent effective lengths, enabling the use of simplified beam models even when significant 3D effects are present. Yee and Melchers [4] further contributed by proposing a method to define equivalent T-stubs for both column flanges and end plates, facilitating the modelling of bolted connections. They also developed empirical relationships, calibrated against experimental data, capable of capturing the full force–displacement response of extended end-plate connections. Additional models for predicting the resistance of T-stub flange plates were later introduced by Kulak et al. [5] and Jaspart [6]. Together with earlier studies, these works form the theoretical foundation of the formulations adopted in Eurocode 3, Part 1–8 [1]. Although the models proposed by Kulak and Jaspart are conceptually similar and based on equilibrium of forces and moments, they differ mainly in their assumptions regarding the location and distribution of bolt forces beneath the bolt head, as well as in the positioning of plastic hinges within the flange plate. During the 1990s, further research addressed not only resistance but also stiffness and ductility of bolted T-stubs. In particular, Swanson [7] and Faella et al. [8], building upon earlier contributions such as those by Faella et al. and Swanson and Leon, developed models capable of reproducing the entire force–displacement response up to failure. Swanson [9] proposed a multilinear incremental model primarily intended for implementation in numerical analysis software. This formulation incorporates, albeit approximately, nonlinear material behaviour, variable tensile stiffness of bolts, and elastic–plastic hinges to represent plate yielding. However, the model presents several limitations: it is sensitive to strain-hardening parameters and bolt ductility, relies on simplified representations of plastic hinge behaviour, and does not define failure strains for plate and bolt materials according to a standardized procedure. Moreover, the bolt force is assumed to act at a single point, consistent with the approach by Kulak et al. [5], which neglects the actual distribution of contact stresses beneath the bolt head in the equilibrium formulation. More advanced incremental models were later introduced to reproduce the full force–displacement response up to collapse, explicitly accounting for material nonlinearity and deformation evolution [3,10]. In particular, refined mechanical

formulations incorporating material strain-hardening and compatibility conditions improved the consistency of flange bending resistance and prying action modelling, albeit at the cost of iterative numerical procedures and extensive conditional checks [2,18]. In parallel, simplified design-oriented expressions have been proposed to estimate the displacement capacity in selected failure mechanisms by linking plastic hinge rotation and bolt elongation [13]. More recently, refined classifications have been introduced to capture intermediate mechanisms frequently observed in experiments, including modes characterised by bolt rupture combined with moderate flange plasticity, which can decisively affect both resistance and deformation capacity [17].

Despite this substantial progress, two main limitations remain and motivate the present work. First, the Eurocode-based component method provides reliable plastic strength and stiffness predictions but does not offer predictive formulations for deformation capacity, preventing an explicit verification of ductility requirements within performance-based framework [1]. Second, while advanced mechanical models can reproduce full-range behaviour with satisfactory accuracy [10–12], they typically require incremental/iterative procedures, making them less suitable for closed-form design applications. Conversely, simplified analytical approaches are often limited to specific collapse mechanisms and do not consistently treat, within a unified framework, key interacting effects such as flange plastic rotation, bolt elongation, and the position of the resultant contact/prying force, which is known to influence both collapse mode identification and ultimate response [12,13]. In addition, intermediate collapse mechanisms (e.g., the transition modes identified in refined classifications [14–17]) are not always captured through simplified yet mechanically coherent expressions that provide reliable predictions of both ultimate resistance and deformation capacity [14]. This conceptual difference between the Eurocode idealisation and the actual response of bolted T-stubs is schematically illustrated in Fig. 2.

Within this framework, in order to address these limitations, this study proposes a simplified analytical formulation for bolted T-stubs aimed at predicting in a simple form suitable for a straightforward calculation both ultimate resistance and deformation capacity of bolted T-stubs within a framework suitable for a practical implementation easily adaptable to a code standard. The model builds upon the refined failure mode classification recently introduced in [14] and further extends it by explicitly accounting for transitional collapse mechanisms between flange-dominated and bolt-dominated behaviour. In particular, the formulation incorporates intermediate mechanisms that enable a smoother transition between the classical collapse modes and provide a more consistent interpretation of experimental observations. In addition, the model introduces a mechanically consistent description of the contact interaction by adopting an analytical evaluation of the position of the resultant contact force [13]. On this basis, closed-form expressions are developed for the governing failure modes to estimate (i) the

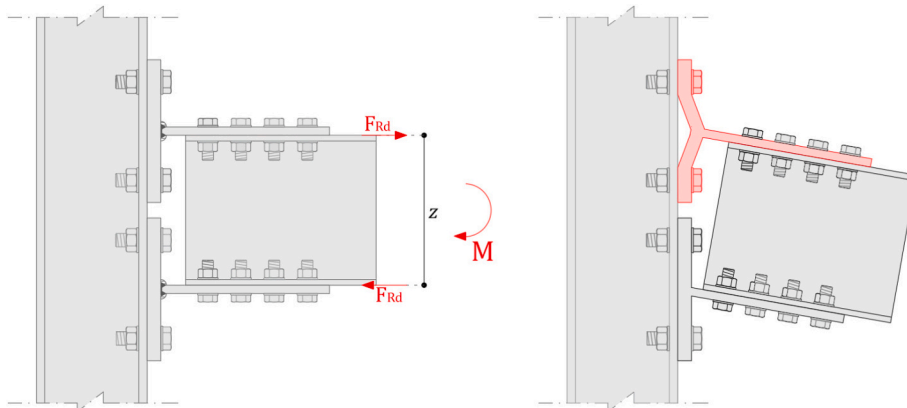


Fig. 1. Effect of a bending moment in a double split Tee joint.

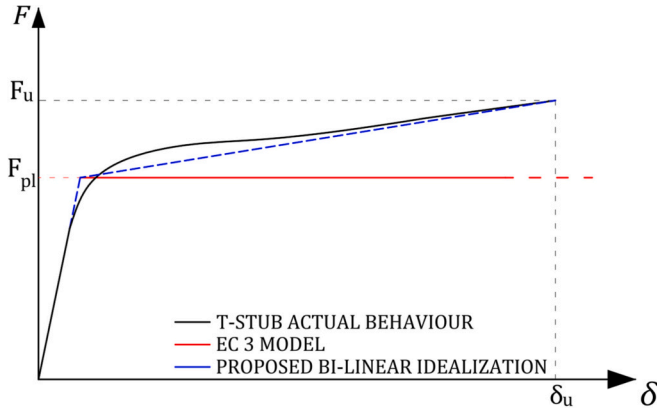


Fig. 2. Conceptual comparison between the rigid-perfectly plastic response assumed in Eurocode 3 and the actual force-displacement behaviour of a bolted T-stub.

ultimate strength, accounting for the relevant component capacities (flange and bolt) and strain hardening effects where appropriate [10–12], and (ii) the ultimate deformation capacity, expressed as the combination of elastic deformation, flange plastic rotation, and bolt elongation at failure [11,18]. The proposed formulation is intended for conventional coupled T-stubs representative of beam-to-column joint components and for geometrical configurations falling within the range considered in the validation dataset. Conversely, isolated T-stubs tied to rigid bases, as well as configurations in which additional large-displacement mechanisms become dominant, are outside the intended field of application of the model. The proposed equations are first verified with a parametric investigation based on an established mechanical model [12] and subsequently validated against experimental results available in the literature [14,19–26]. Reference values for bolt fracture strain are adopted when direct test data are unavailable, consistently with published experimental evidence on high-strength bolts [27–29].

The paper is organised as follows. Section 2 reviews the Eurocode 3 formulation and the main mechanical models available in the literature for the closed form assessment of resistance and deformation capacity. Section 3 presents the proposed analytical model and the derivation of the closed-form expressions for ultimate resistance and deformation capacity. Section 4 describes the parametric investigation performed to verify the proposed formulation and the model validation against experimental data. Finally, the main conclusions are summarised in Section 5.

2. Existing models

2.1. Eurocode 3 model

According to Eurocode 3, Part 1–8 [1], the plastic resistance of bolted T-stubs can be evaluated by identifying three possible mechanisms, namely type-1 (MEC-1), associated with flange yielding, type-2 (MEC-2), involving combined flange yielding and bolt failure, and type-3 (MEC-3), governed by bolt failure. These mechanisms, schematically illustrated in Fig. 3, are defined within the framework of a rigid-plastic theory. In MEC-1, four plastic hinges are assumed to form, two located at the bolt axes and two at the flange-to-web connections. MEC-2 is characterised by the formation of plastic hinges at the flange-to-web connections combined with the bolts plasticisation. Finally, MEC-3 corresponds to bolt rupture without significant yielding of the flange.

The plastic resistance corresponding to each mechanism is given by:

$$F_{Rd,1} = \frac{4M_{pl}}{m} \quad (1)$$

$$F_{Rd,2} = \frac{2M_{pl} + 2B_{Rd}n}{m + n} \quad (2)$$

$$F_{Rd,3} = 2B_{Rd} = 2 \cdot 0.9A_{res}f_{ub} \quad (3)$$

where $M_{pl} = (b_{eff}t^2f_y)/4$ and $B_{Rd} = 0.9A_{res}f_{ub}$, with t denoting the plate thickness, b_{eff} the effective length of the T-stub flange [2], m the distance between the bolt axis and the plastic hinge forming at the flange-to-web connection, e the distance between the bolt axis and the plate tip, n the parameter defining the location of the prying forces, A_{res} is the net area of the bolts, f_y is the design yield stress of the plate material, and f_{ub} is the ultimate stress of the bolts. In the EC3 formulation, the prying forces are assumed to be located at the tip of the plate, and therefore $e = n$ with $n \leq 1.25m$. The plastic resistance of the T-stub is then evaluated as the minimum among the three possible failure modes:

$$F_{Rd} = \min\{F_{Rd,1}; F_{Rd,2}; F_{Rd,3}\} \quad (4)$$

Based on Eqs. (1)–(3), the failure modes can also be identified through the definition of the non-dimensional parameter $\beta = 2M_{pl}/B_{Rd}m$, namely the ratio between MEC-1 and MEC-3 resistance:

- MEC-1 if $\beta \leq \beta_{1-2}$;
- MEC-2 if $\beta_{1-2} < \beta < \beta_{2-3}$;
- MEC-3 if $\beta \geq \beta_{2-3}$.

with $\beta_{1-2} = 2\lambda/(1 + 2\lambda)$ and $\beta_{2-3} = 2$, with $\lambda = n/m$. As the formulas proposed in EC3 are derived from a rigid-plastic theory, they are pri-

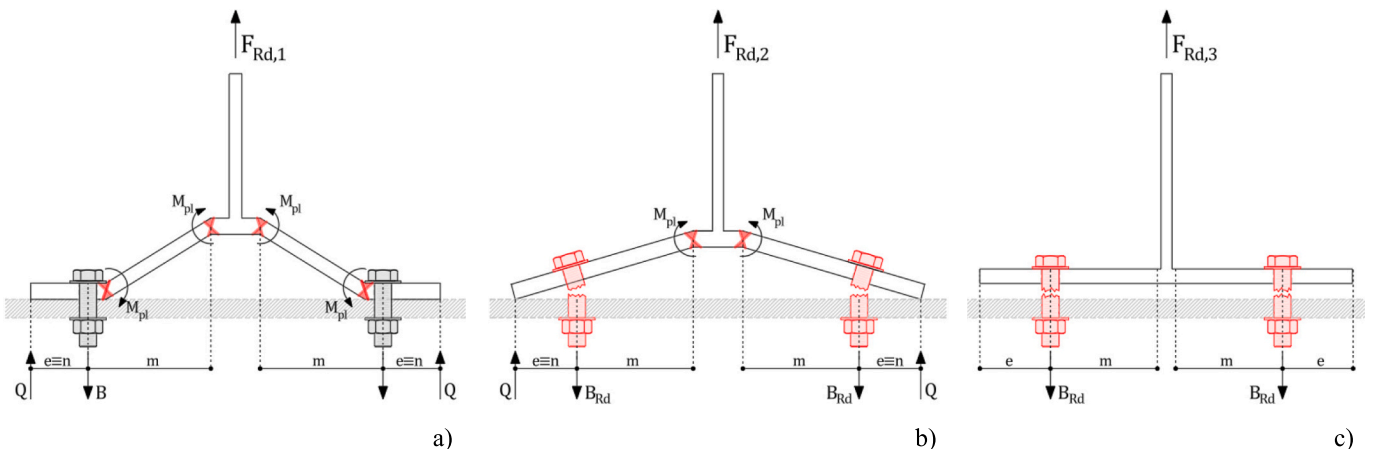


Fig. 3. T-stub failure modes according to EN 1993-1-8 [1].

marily aimed at evaluating the plastic strength. Therefore, these approaches do not provide any information on the ultimate resistance of the joint. However, the prediction of the ultimate resistance becomes especially relevant when the connection is weaker than the beam, namely in the case of partial-strength joints, since under certain loading conditions the available plastic reserves may be activated. Furthermore, although yielding may first occur in a specific component, the ultimate failure mechanism is not necessarily controlled by that component. Indeed, owing to strain hardening and the resulting plastic redistribution, a different component may become critical at a more advanced stage of deformation. Therefore, a reliable assessment of both the ultimate resistance and the deformation capacity is crucial in many analytical applications.

In this context, Eurocode 3 Part 1–8 provides no explicit guidance on the prediction of ultimate resistance and deformation capacity. The only provision related to the post-plastic behaviour of bolted T-stubs is given in Clause 6.4.2, where a bolted joint is indicated to be sufficiently ductile if the following inequality is satisfied:

$$t \leq 0.36d \sqrt{f_{ub}/f_y} \quad (5)$$

with d equal to the bolt diameter. This expression originates from [6] and is based on the assumption that a bolted T-stub can be considered ductile only when failure takes place according to MEC-1 and, less conservatively, when the condition $\beta \leq 1$ is met. Nevertheless, the range of validity of this inequality remains unclear, as the notion of sufficient ductility is inherently dependent on the purpose of the analysis. While the above condition may be acceptable when the objective is to enable structural plastic redistribution in the framework of a plastic analysis, which requires only limited plastic deformation, it is certainly insufficient to guarantee the ductility of a bolted joint under seismic loadings or robustness-related design situations. It should be stressed that Eq. (5) is not a predictive expression for the deformation capacity of a T-stub. It represents a ductility requirement within the Eurocode 3 framework, mainly intended to identify configurations that may provide sufficient ductility for plastic analysis. Therefore, it cannot be expected to correlate directly with the ultimate displacement δ_u or ductility demand resulting from a specific loading scenario.

2.2. Existing formulations for the closed-form prediction of ultimate strength and deformation capacity

The limitations of the Eurocode 3 Part 1–8 formulation [1], particularly with regard to the prediction of ultimate strength and deformation capacity, have progressively motivated the development of mechanical models of increasing sophistication. The prediction of the ultimate behaviour of bolted T-stubs has evolved from simplified post-yield idealizations to more refined formulations capable of describing both ultimate resistance and deformation capacity. The earliest contribution by Jaspart [6] and Jaspart & Maquoi [30] addressed the lack, within design standards such as Eurocode 3, of a clear description of the

post-limit response of individual joint components. Their approach models the component response by means of a bilinear law (Fig. 4), in which the post-yield branch is governed by a post-limit stiffness associated with material strain hardening. Within this framework, the ultimate deformation is obtained by combining the elastic contribution up to design resistance with the post-limit deformation developed between design and ultimate resistance (see Table 1). The method is simple and consistent with the component method philosophy, but its predictive capability remains limited because the actual failure mechanism of the T-stub, the interaction between bolts and flange, and the evolution of prying forces are not explicitly represented.

A more mechanically grounded interpretation was subsequently proposed by Faella et al. [8], who described the ultimate response of the T-stub by identifying the relevant collapse mechanisms and evaluating the associated plastic deformations in the flange. In their model, the ultimate condition is governed by the relative magnitude of the flange bending resistance and the bolt tensile resistance, which gives rise to the classical three failure modes. Once the governing mechanism has been identified, the ultimate displacement is determined by integrating the curvature distribution along the flange and by evaluating the plastic rotations of the hinges involved in the collapse mechanism. Accordingly, the model does not simply prescribe an ultimate deformation value; rather, it derives this value from the assumed stress-strain law of the flange material and the corresponding moment-curvature relationship.

This constitutes a significant advancement, since the ultimate behaviour is directly related to the mechanics of collapse. It is also worth noting that, although the model proposed by Faella et al. [8] was conceived as an incremental approach for describing the full-range response of bolted T-stubs, it also enables a closed-form evaluation of both the ultimate resistance and the deformation capacity (see Table 1) through the introduction of the parameter ψ_{Faella} , defined as the ratio between the bending moment acting, at the ultimate condition, at the flange-to-web connection and the bending moment acting at the bolt:

$$MEC-1 : \psi_{Faella} = 1; MEC-2 : \psi_{Faella} = \frac{\lambda(2\xi - \beta)}{(1 + \lambda)\beta}; MEC-3 : \psi_{Faella} = 0 \quad (6)$$

where $\xi = M_{pl}/M_u$, namely the ratio between the plastic resistance and ultimate bending resistance of the plate.

The formulation also introduces parameter C , which depends exclusively on the material properties and derives from the integration of the curvatures over a cantilever idealisation of the flange plate, considering either the region between the bolt line and the point of zero bending moment or the region between the flange-to-web connection and the point of zero bending moment (Fig. 5). Although this parameter can, in principle, be derived in closed form through integration, Faella et al. also provided recommended values for typical structural steel grades based on assumed material properties, namely $C = 0.12$ for S235 steel, $C = 0.20$ for S275 steel, and $C = 0.33$ for S355 steel [11]. These values are therefore only indicative and should be used with caution, as they rely on specific assumptions regarding the material. Whenever accurate material information is available, those assumptions should be avoided, since the model is particularly sensitive to the ultimate fracture strain, defined as the fracture logarithmic strain $\ln(A_0/A_f)$, which represents a key parameter of the model (A_0 is the original coupon cross-sectional area and A_f is the cross-sectional area at fracture).

Faella et al. model while advancing significantly the existing literature, it still relies on several simplifying assumptions, including a two-dimensional representation, the fixed prying force location, neglect of bolt bending, and the absence of a rigorous compatibility relationship between bolt and flange deformations. As a result, as it was already demonstrated in past works its predictions are typically inaccurate for MEC-2, where the bolt contribution becomes dominant [19]. Indeed, the model assumes the simultaneous attainment of the ultimate rotation in the plastic hinge at the plate-to-web attachment and bolt failure. As this

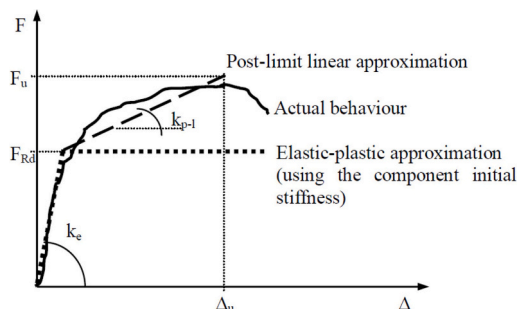


Fig. 4. Jaspart & Maquoi [30] post-yielding model.

Table 1
Main features of existing models [6,11,14,18].

		MEC-1	MEC-1.2	MEC-2	MEC-3
Beg et al. [18]	Range of β	$\beta \in \left[0; \frac{2\lambda}{1+2\lambda}\right]$		$\beta \in \left[\frac{2\lambda}{1+2\lambda}; 2\right]$	$\beta \in [2; \infty]$
	F_u	$\frac{4M_{pl}}{m}$		$\frac{2M_{pl} + 2B_{Rd}n}{m+n}$	$2B_{Rd}$
	δ_u	$0,4 \bullet m$		$0,1 \bullet l_b \left(1 + k \frac{m}{n}\right)$	$0,1 \bullet l_b$
Faella et al. [8]	Range of β	$\beta \in \left[0; \frac{2\lambda\xi_F}{1+2\lambda}\right]$	–	$\beta \in \left[\frac{2\lambda\xi_F}{1+2\lambda}; 2\xi_F\right]$	$\beta \in [2\xi_F; \infty]$
	F_u	$\frac{4M_{u,F}}{m}$		$\frac{2M_{u,F} + 2B_{Rd}n}{m+n}$	$2B_{Rd}$
	δ_u	$\frac{F_u}{K_e} + C \frac{m^2}{2t}$		$\frac{F_u}{K_e} + C \frac{m^2}{(1+\psi)t}$	$\frac{F_u}{K_e} + \varepsilon_{ub} l_b$
Jaspart et al. [6]	Range of β	$\beta \in \left[0; \frac{2\lambda}{1+2\lambda}\right]$		$\beta \in \left[\frac{2\lambda}{1+2\lambda}; 2\right]$	$\beta \in [2; \infty]$
	F_u	$\frac{4M_{u,J}}{m}$		$\frac{2M_{u,J} + 2B_{Rd}n}{m+n}$	$2B_{Rd}$
	δ_u			$\frac{F_{pl}}{K_e} + \frac{F_u - F_{pl}}{K_{pl}}$	$[2; +\infty]$
Lyu et al. [14]	Range of β	$\left[0; \frac{2\lambda\xi}{1+2\lambda}\right]$	$\left[\frac{2\lambda\xi}{1+2\lambda}; \frac{1}{\eta} \left(\sqrt{1 + \frac{4\lambda\eta}{1+2\lambda}} - 1\right)\right]$	$\left[\frac{1}{\eta} \left(\sqrt{1 + \frac{4\lambda\eta}{1+2\lambda}} - 1\right); 2\right]$	
	F_u		$\frac{4M_{u,P}}{m}$	$2B_{Rd} \frac{\lambda(1+\psi)}{\lambda(1+\psi) + \psi}$	$2B_{Rd}$

Where l_b is the bolt length defined according to Eurocode 3 Part 1–8 [11], ε_{ub} the ultimate strain of the bolt, k is an empirical factor with values between 1 and 5, $\xi = M_{pl}/M_u$, $\xi_F = M_{pl}/M_{u,F}$, $M_{u,J} = b_{eff} t^2 f_u / 4$, C is a material parameter [8], $M_{u,F}$ is the ultimate bending moment evaluated according to [8], K_e and K_{pl} are the elastic and plastic stiffnesses of the T-stub, respectively and $\eta = (M_{u,F} - M_{pl})/M_{pl}$.

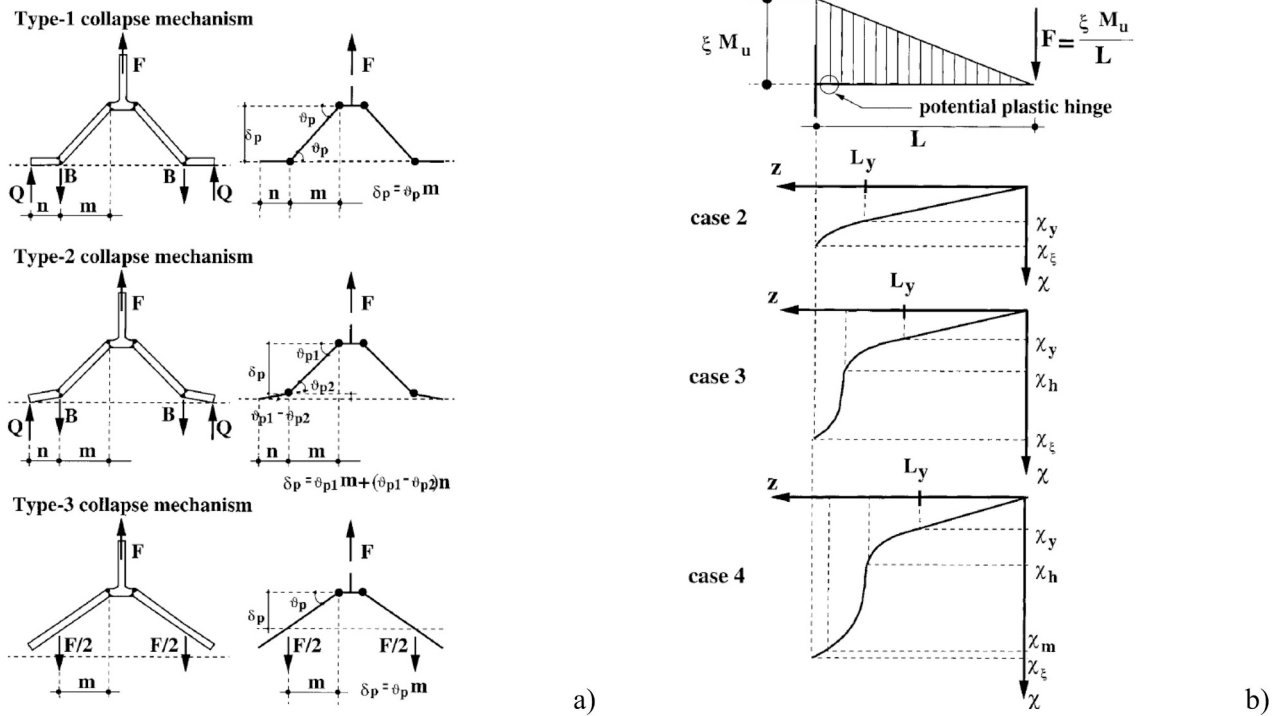


Fig. 5. Methodology for the evaluation of T-stubs displacements starting from the curvature according to [8]: a) assumed failure modes; b) computation of plastic hinge rotations.

assumption is not representative of the actual response, it results in a marked overprediction of the deformation capacity.

Further refinement was introduced by Swanson & Leon [31], who developed a nonlinear analytical model aimed at reproducing the entire force-displacement response up to failure. In this formulation, ultimate behaviour emerges from the interaction between a nonlinear bolt spring,

partially plastic flange hinges, and membrane effects developing in thin flanges. The bolt response is described through a multilinear force-deformation relationship, while the flange is modelled with an elastic-yielding-plastic constitutive law (Fig. 5). The prediction of failure is obtained incrementally by checking, at each stage, whether the controlling limit state is governed by bolt force or by stresses within the

flange. This approach allows multiple changes in stiffness and a more realistic description of the path to failure, but it also introduces significant complexity. For this reason, the model is not suitable for manual design applications and remains strongly dependent on the assumed bolt ductility and strain-hardening parameters.

The model proposed by Girão Coelho [25] follows a similar mechanical rationale but moves more explicitly into a finite-element-based formulation. The flange material is described through a multilinear true stress-logarithmic strain relationship, while bolt elongation is derived from the Swanson and Leon approach. Ultimate behaviour is assumed to be reached when either the extreme fibre of the flange attains its ultimate strain or the bolt fails in tension. In this case, the prediction of ultimate response depends on numerical simulation rather than on closed-form expressions, which improves flexibility and accuracy but reduces practicality for routine use. The main contribution of this model is therefore methodological: it shows that a more refined representation of material nonlinearity and distributed plate response can reproduce experimental behaviour more satisfactorily than simplified analytical models.

Beg et al. [18] proposed a design-oriented method for predicting deformation capacity (see Table 1). Rather than deriving the ultimate response from a full mechanical model, they defined direct expressions for the deformation capacity associated with each of the EC3 failure modes starting from a very simple assessment of the plate deformation which is separated in two contributions: a contribution due to the rotation of the plastic hinges and a contribution due to the bolt axial elongation (Fig. 6). For flange-controlled failure (MEC-1), the ultimate displacement is estimated from the assumed plastic hinge rotation capacity, itself obtained by taking the hinge length equal to the flange thickness and adopting a prescribed ultimate strain for the plate equal to 0.20. For mixed and bolt-controlled modes (MEC-2 and MEC-3), the deformation capacity is related to bolt elongation and to empirical assumptions on the relative rotation of the hinges. This approach is attractive because it is straightforward and immediately applicable in design, but its simplicity comes at the cost of accuracy. In particular, ultimate strength is not independently predicted, as it is assumed equal to the plastic resistance from EC3, and the deformation model remains strongly dependent on empirical choices.

A more advanced incremental model was later developed by Lemonis & Gantes [32]. Their formulation still represents the flange as a beam and the bolt as a deformational spring, but it improves the description of ultimate behaviour by explicitly accounting for contact evolution and partial separation between the flange and the support. Instead of assigning prying forces to a fixed position, the model determines the separation point within the contact zone during the loading process. The ultimate response is then obtained incrementally by updating bending moments in the flange, bolt force, plasticisation lengths, and bolt stiffness until either flange fracture or bolt failure is detected. This is an important improvement because the evolution of contact strongly

influences both resistance and displacement capacity. Optional refinements can also account for bolt head size, bolt bending, and three-dimensional effects. Although this model offers a more realistic physical interpretation of ultimate behaviour, its complexity prevents closed-form solutions and makes it more appropriate for computational implementation than for hand calculations.

The work of Francavilla et al. [10,12] can be seen as a further development of the mechanical approach originally proposed by Faella et al. [8]. Their model improves the prediction of ultimate behaviour by introducing a more rigorous treatment of bolt-plate compatibility, material nonlinearity in both plate and bolts, and explicit failure checks based on the ultimate strain of the basic materials. The displacement of the T-stub is evaluated step by step as the sum of elastic and plastic contributions, while the contact force position is derived from the deformed configuration of the plate rather than assumed a priori. This leads to a much more accurate prediction of both resistance and ultimate displacement, especially in cases where bolt behaviour plays a key role. In the later extension, geometric nonlinearity and additional large-displacement failure modes were also included, such as bolt shear failure, hole bearing, frictional effects, and catenary action in the plate. These developments significantly enhance the realism of the model and its ability to reproduce experimental moment-rotation behaviour, but they also make the method computationally demanding and unsuitable for simplified design approach.

Finally, Lyu et al. [14] proposed a comprehensive mechanical model capable of reproducing the full-range response of bolted T-stubs. The model accounts for strain hardening in the flange and for both strain hardening and softening in the bolts, thus enabling the prediction not only of the ultimate resistance but also of the complete force-displacement response up to fracture. The T-stub displacement is evaluated by combining flange deformation and bolt elongation, both derived through constitutive integration procedures. While the resistance can be evaluated in closed form, the ultimate displacement is not available in closed form and must be determined through an incremental procedure (Table 1). Despite some simplifying assumptions, such as the neglect of three-dimensional effects and geometric nonlinearity, the model represents one of the most comprehensive analytical tools currently available for predicting the ultimate behaviour of bolted T-stubs. A major contribution of this model is the introduction of an extended classification of failure modes, which supplements the three classical mechanisms with an additional mode involving bolt rupture combined with the formation of four plastic hinges in the flange. This mechanism, frequently observed in experimental tests during the post-yielding phase, is denoted as MEC-1.2 and represents an intermediate transition between MEC-1 and MEC-2. The approach proposed by Lyu et al. [14] is therefore based on an alternative definition of the parameter ψ with respect to that introduced by Faella et al. [8].

In particular, it was observed that the original definition proposed by Faella et al. does not adequately capture the actual structural response.

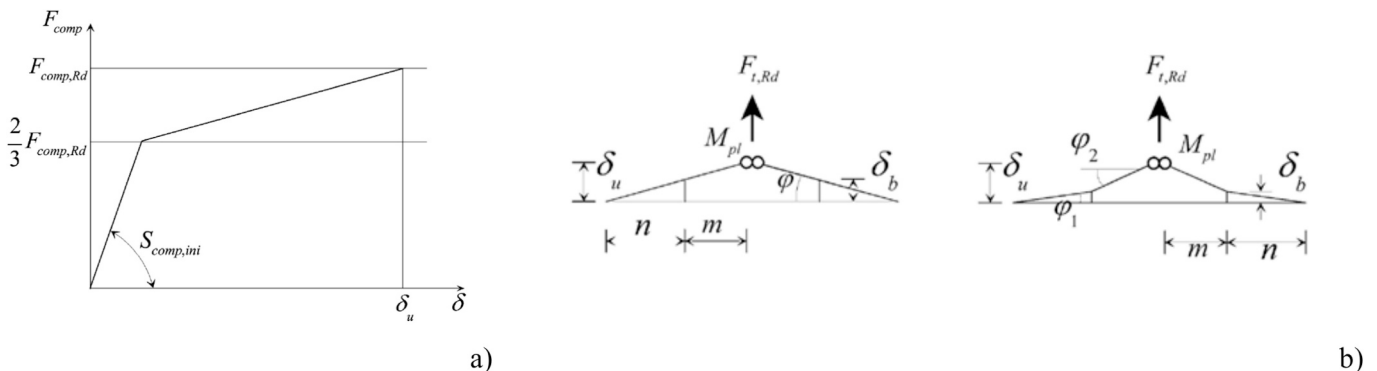


Fig. 6. a) Bi-linear model proposed by Beg [18]; b) Proposed kinematic mechanisms at failure by Beg [18].

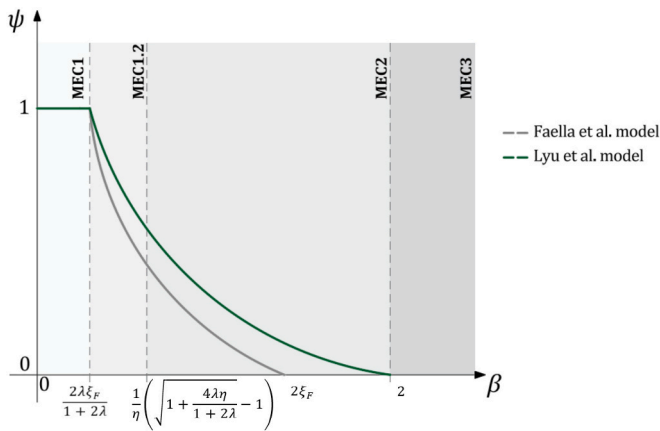


Fig. 7. ψ vs β in the framework of a four-modes classification.

Therefore, in order to describe the transition from MEC-1 to MEC-2, Lyu et al. assume that ψ varies linearly with $1/\beta$ in the range $\beta_1 \leq \beta \leq \beta_3$, namely (Fig. 7):

$$\begin{aligned} \text{MEC} - 1 : \psi_{\text{Lyu}} &= 1; \text{MEC} - 1.2 \text{ and } \text{MEC} - 2 : \psi_{\text{Lyu}} \\ &= \frac{\lambda(2 - \beta)}{\left(\frac{1+2\lambda}{\xi} - \lambda\right)\beta}; \text{MEC} - 3 : \psi_{\text{Lyu}} = 0 \end{aligned} \quad (7)$$

Table 1 summarizes the equations proposed by the models currently available for the prediction of the ultimate resistance and deformation capacity of bolted T-stubs. Based on these formulations, which are derived from equilibrium and compatibility conditions, it is also possible to identify the corresponding ranges of application for the different failure modes. This highlights a preliminary but essential issue, namely the classification of the governing failure mode, which is required for an accurate prediction of both ultimate strength and deformation capacity.

The classification of the failure mode of a T-stub is generally based on the parameter β , through the definition of threshold values separating the various mechanisms (Fig. 7) (β_1 and β_3 in the models by Jaspart et al., Beg and Faella et al.; β_1 , $\beta_{1.2}$, and β_3 in the model by Lyu et al.). For each of the authors considered, these threshold values are also reported in Table 1.

2.3. Accuracy of existing models

A database of 58 experimental tests on bolted T-stubs was collected from previous studies and used as a reference benchmark. The experimental cases were selected to be representative of current structural configurations and to be consistent with the modelling assumptions which are essentially referred to the case of short T-stubs [33], for which the effective length corresponds to the actual width. The experimental dataset includes tests reported by a number of different authors, namely: Faella et al. [8], Anwar [20], Liang et al. [21], Lin et al. [15], Ydstebø [23], Zhao et al. [15], Sabatino [34], Bursi et al. [24], Girão Coelho [25] and Liu et al. [35] (see Annex A). Some tests presented features that made a direct comparison with the analytical model less straightforward and therefore they were not considered in the validation dataset. In particular, tests in which slip in the jaws [19], premature weld failure [21,25], as well as cases in which failure of the specimen was not reached [21], were excluded. Additionally, tests with geometries not representative of practical structural applications (e.g. small thickness of the flange), or significantly influenced by large-displacement response due to catenary actions or shear in the bolts (isolated T-stubs connected to rigid base) were excluded. It should be emphasized that the exclusion of these tests does not imply that MEC-1 configurations or specimens exhibiting significant deformation capacity were disregarded. The proposed formulation explicitly accounts for MEC-1 behaviour and was

validated against both experimental results and parametric simulations including flange-dominated failure modes and cases characterised by large deformation capacity. The distinction is instead related to the T-stub configuration considered. The proposed model is intended for coupled T-stubs and not for isolated T-stubs tied to rigid bases. In the latter case, large-displacement response may activate additional mechanisms that are not present, or are significantly less relevant, in coupled T-stubs, such as bolt shear and pronounced catenary action of the flange. These mechanisms may significantly influence the ultimate response and would require specific modelling assumptions and corresponding predictive relationships for both resistance and deformation capacity. Conversely, for coupled T-stubs, bolt shear forces cannot develop in the same manner and the influence of geometric nonlinearities is considerably reduced. Catenary effects may arise only at very large deformation levels, which are rarely attained in practical applications and are generally associated with unusual geometrical configurations. Although second-order geometric effects and geometric nonlinearities are not explicitly included in the closed-form analytical formulation presented in this work, their influence was indirectly assessed through the extensive validation carried out in the paper. In particular, the comparison herein presented with 58 experimental tests and 138 simulated parametric cases did not show any clear degradation of the model accuracy for configurations where such effects could potentially become more relevant. Therefore, within its stated range of applicability, namely coupled T-stubs with geometries falling within the validation range considered in this study, the proposed formulation provides an accurate prediction of the response with the level of approximation documented in the paper. The modelling of isolated T-stubs connected to rigid supports is currently the subject of ongoing research and falls outside the scope of the present study.

The distribution of the tests among the different authors is summarised in Table 2.

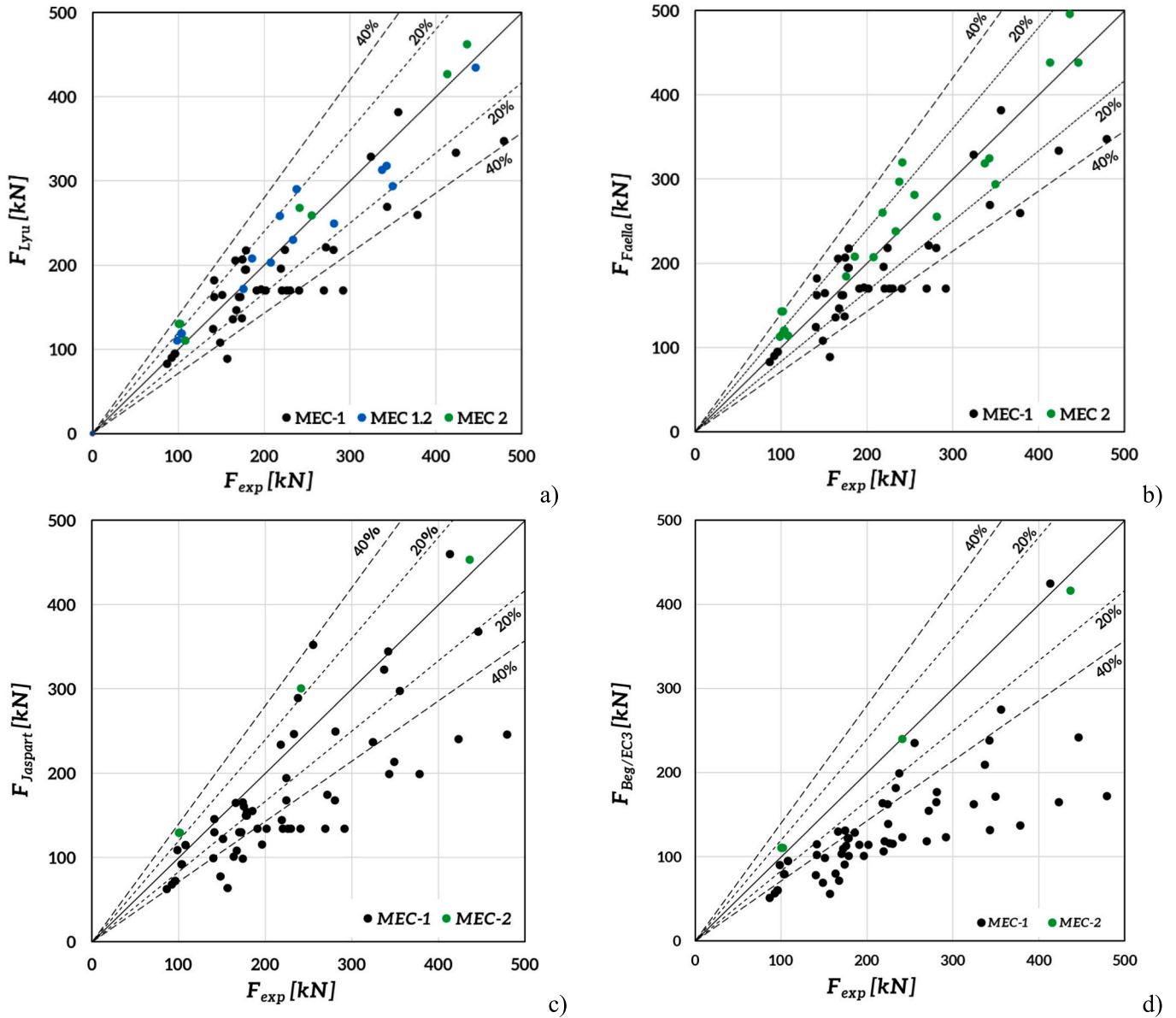
The collected experimental results were compared with the closed-form analytical models previously presented in terms of both ultimate resistance (Fig. 8) and ductility (Fig. 9), namely those proposed Faella et al. [3], Jaspart [9], Beg et al. [13], and Lyu et al. [17] (Table 1). Based on the classification method proposed by Lyu et al., 40 tests are identified as MEC-1, 13 as MEC-1.2 and 5 as MEC-2. The comparison between the experimental results and the predictions of the closed-form formulations highlights that the accuracy of the prediction depends not only on the statistical agreement in terms of mean values and coefficients of variation, but also on the capability of each model to reproduce the actual mechanics governing the ultimate response of bolted T-stubs. In this respect, the analysed models show markedly different levels of accuracy, especially when moving from ultimate resistance to ultimate deformation capacity, which is much more sensitive to the modelling assumptions adopted for flange fracture, bolt elongation, and flange-bolt interaction.

With regard to the prediction of *ultimate resistance*, the best overall agreement is obtained with the formulations by Lyu et al. and Faella et al., both of which provide average values of $F_u/F_{u,\text{exp}}$ close to unity, namely 0.96 and 0.98, respectively (Table 3). This confirms that both models are able to capture, with satisfactory accuracy, the main equilibrium conditions governing collapse. Among the two, however, the model by Lyu appears to provide the most robust representation of the ultimate strength, since it combines a refined failure mode classification with an improved description of the transition between different mechanisms. In particular, the introduction of the intermediate mechanism MEC-1.2 allows the model to describe cases in which bolt rupture occurs before the plate collapse, a response that is frequently observed experimentally and cannot be properly represented within the traditional three-mode classification which were initially derived to predict the plastic resistance. This results in a more accurate identification of the governing mechanism and, consequently, in a better estimation of collapse resistance. Nevertheless, some limitations remain, especially in the transition from MEC-2 to MEC-3, which is still described in a

Table 2

Experimental database.

	Faella et al	Anwar	Liang et al.	Lin et al.	Ydstebo et al.	Zhao et al	Sabatino	Bursi et al.	Girão Coelho	Liu et al.
Number of tests	11	8	8	4	3	5	4	4	7	7

**Fig. 8.** Ultimate resistance - Comparison between experimental results and predictions according to: a) Lyu; b) Faella; c) Jaspart; d) Beg and EN 1993-1-8.

simplified way and may not fully capture the gradual transition observed in experimental studies, such as those reported by Tartaglia et al. [16].

More conservative results are obtained with the Jaspart model, which gives an average value of $F_u/F_{u,exp} = 0.81$ (Table 3). This underestimation is not surprising, since the model is based on a highly simplified idealisation of the post-yield response. Its main merit lies in introducing a closed-form post-limit description consistent with the component method philosophy, but the formulation does not explicitly model the actual failure mechanism of the T-stub, nor the interaction between the flange and the bolts. In particular, the resistance of the plate is evaluated through a simplified ultimate bending resistance based on the material response up to f_u , without a detailed description of the

actual stress redistribution and strain hardening developing in the flange near collapse. As a result, the model retains a certain predictive capability for resistance, but it is inherently approximate and systematically conservative. The most conservative predictions of ultimate resistance are provided by the Beg formulation, for which the average ratio is equal to $F_u/F_{u,exp} = 0.65$ (Table 3). This behaviour is directly related to the modelling assumptions. In fact, the resistance is not derived from an independent mechanical prediction of the ultimate state, but is essentially taken as the plastic resistance consistent with the Eurocode 3 component method. Therefore, the beneficial effects associated with strain hardening and with the actual evolution of the flange mechanism are not captured.

If the prediction of resistance remains, overall, reasonably

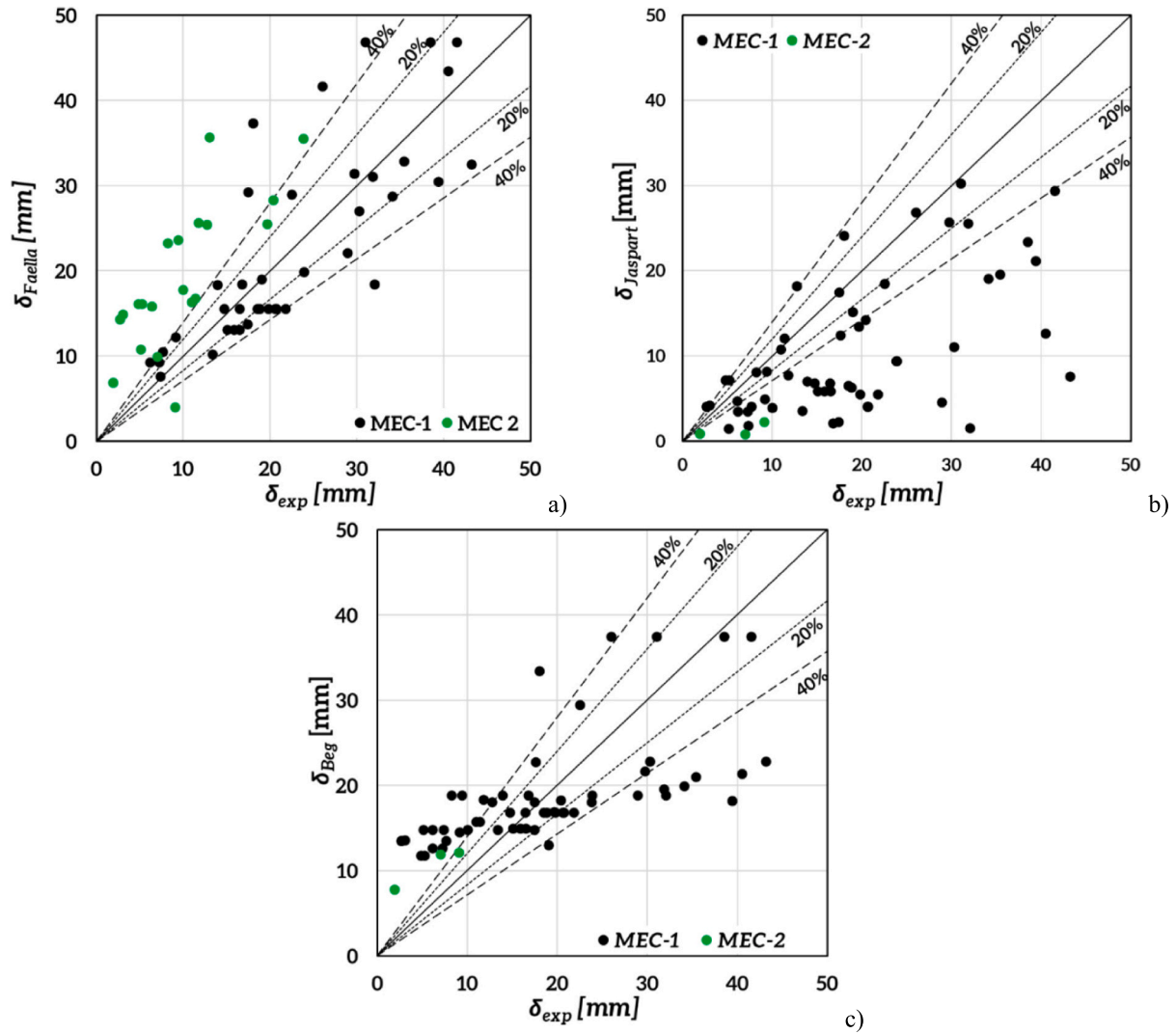


Fig. 9. Ultimate deformation capacity - Comparison between experimental results and previsions according to: a) Faella; b) Jaspart; c) Beg.

Table 3
Performance of literature models.

	$F_u/F_{u,exp}$	CoV	δ/δ_{exp}	CoV
Faella et al.	0.98	0.20	1.61	0.69
Lyu et al.	0.96	0.19	–	–
Jaspart et al.	0.81	0.28	0.62	0.63
Beg	0.65	0.28	1.48	0.73

satisfactory for the most refined formulations, the comparison becomes much more critical when considering the *ultimate deformation capacity*. This is expected, since collapse displacement depends on a more complex interaction between material fracture, plastic hinge development, bolt elongation, and the compatibility between plate uplift and bolt deformation. Accordingly, even models that predict resistance quite accurately may fail to reproduce the actual displacement capacity.

The Faella et al. model is a clear example of this point. For flange-governed failures, the formulation provides very good predictions (MEC-1), because the displacement capacity is obtained by integrating the moment–curvature relationship of the plate up to the attainment of the ultimate strain in the flange. In these cases, the model is fully coherent with the mechanics of collapse and leads to accurate estimates. However, when the governing mechanism is influenced by bolt rupture, and especially for MEC-2, the predictions deteriorate significantly,

leading to an average value of $\delta/\delta_{exp} = 2.59$ with a very large scatter.

The reason is well identified in the basic assumptions of the model. In MEC-2, the formulation assumes the simultaneous attainment of bolt failure and of the ultimate rotation in the plastic hinge at the plate-to-web attachment. This assumption is not representative of the actual structural response. In reality, bolt rupture and flange rotation do not generally reach their ultimate values at the same instant. The behaviour of the Jaspart model is almost opposite. While it shows a moderate capability in predicting ultimate resistance, it performs poorly in terms of deformation capacity, with an average ratio $\delta/\delta_{exp} = 0.62$ and a large coefficient of variation. This result is a direct consequence of the extreme simplification adopted in the post-limit representation. The Beg model also shows clear limitations in the prediction of deformation capacity. From a conceptual point of view, the procedure is reasonable, since it attempts to express the ultimate displacement as the sum of physically meaningful contributions, namely plastic hinge rotation and bolt elongation. However, the actual implementation remains too approximate. First, the ultimate rotation capacity of the plastic hinge is evaluated by assuming a fixed ultimate plate strain equal to 0.20, which may not be representative of the actual fracture strain of the flange material. This is particularly evident when compared with more mechanically based formulations, such as Faella et al. model, where the ultimate strain is linked to the actual fracture properties of the material through the logarithmic strain at fracture [36]. Second, in the mixed

mechanisms, the relationship between bolt uplift and flange rotation is introduced through the empirical coefficient k , which is allowed to vary over a wide range and was taken equal to 3 (average between 1 and 5) in the present analyses. This parameter has a strong influence on the predicted displacement, but it is not derived from a rigorous compatibility condition. As a result, even if the Beg approach is simple and appealing from a design perspective, it lacks sufficient mechanical control and produces highly scattered and generally unreliable predictions of ultimate displacement.

Overall, the comparison shows that the prediction of ultimate strength is relatively robust when equilibrium-based formulations are combined with an adequate classification of the governing mechanism, whereas the prediction of ultimate deformation capacity remains much more challenging. Simplified models such as those by Jaspart and Beg are useful because they offer immediate closed-form expressions, but they inevitably sacrifice accuracy, especially for displacement capacity. The Faella et al. model represents an important mechanical advancement and performs well whenever flange fracture governs the collapse, but it becomes less reliable when bolt rupture interacts with flange plasticity. The Lyu et al. model appears to offer the most accurate current framework for strength prediction and the most rational basis for the description of mode transition, although some issues may still remain in the representation of the transition towards bolt-dominated failure. These observations confirm that, while closed-form formulations may still be effective for practical estimates of ultimate resistance, the reliable prediction of collapse displacement requires a much more refined treatment of fracture, compatibility, and mechanism transition. Considering the limitations of the models discussed above, the next section presents a novel formulation for the closed-form prediction of both ultimate resistance and deformation capacity. The aim is to provide a model that can be incorporated into a design standard such as EC3 part 1–8, thereby allowing practitioners to adopt a more advanced yet still practical approach for the assessment of bolted T-stubs in design-oriented applications.

3. Closed-form model for ultimate resistance and deformation capacity of bolted T-Stub

The model proposed in this work is conceived to overcome the main limitations of the existing closed-form formulations by introducing a set of targeted refinements, identified on the basis of the comparisons discussed in the previous sections. In particular, the improvement of the prediction of both ultimate resistance and deformation capacity is shown to depend on three key aspects: i) the correct classification of the governing collapse mechanism, which in turn depends also on the accurate estimate of the location of the prying forces; ii) a sufficiently accurate yet still simple way of evaluating the flange ultimate bending resistance and iii) a more realistic definition of the plastic hinge rotations in the different failure modes. With regard to the failure mode identification, the existing models generally assume that the contact force acts at a fixed location, usually at the plate tip, whereas experimental and numerical evidence indicates that its position evolves during loading, being located closer to the bolt head in MEC-1, moving towards the plate edge in MEC-2, and assuming intermediate positions in MEC-1.2 [12,13,37]. Moreover, recent observations have shown that loss of contact may occur for values of β lower than 2 [14,16], which makes it necessary to introduce a further transition mechanism between MEC-2 and MEC-3, together with a revised definition of the parameter λ . As far as resistance is concerned, the proposed formulations adopt an intermediate approach between the excessive simplification of the Jaspart model and the greater complexity of the Faella et al. model, following instead the more balanced strategy suggested by Lemonis and Gantes [32] for the evaluation of the flange ultimate moment. The most innovative part of the proposed model concerns the prediction of the ultimate displacement. For MEC-1, the formulation essentially follows the approach by Faella et al., which has been shown to provide accurate

predictions for flange-governed mechanisms. For MEC-2 and MEC-3, where collapse is controlled by bolt failure, the proposed approach is instead consistent with the rationale of the model by Beg. The main advancements of the present formulation are therefore concentrated in the transition mechanisms, where significant improvements are introduced, particularly for the MEC-1.2 and the novel MEC-2.3 ranges, through specific transition equations verified on the results of an extensive parametric studies and validated on a wide range of experimental tests taken from literature. These aspects are discussed in detail in the following subsections.

3.1. A novel five-modes classification of bolted T-stubs based on the ultimate response

As mentioned before, the classification of T-stub failure modes adopted in EC3 part 1.8 is based on a rigid-plastic idealisation and, for this reason, it should be interpreted primarily as a classification of the plastic collapse mechanisms rather than of the actual ultimate response. In particular, the codified boundaries between the traditional modes are obtained from equilibrium conditions written for plastic mechanisms in which the flange and bolt behaviour are represented in a simplified form. As a consequence, while this framework is effective for plastic design purposes, it does not fully capture the progressive evolution of the real response up to failure, especially when the prediction of both ultimate resistance and deformation capacity is required. Starting from the four modes classification proposed by Lyu et al. [14], the present proposal introduces a revised classification based on the non-dimensional parameter β , aimed at describing more accurately the ultimate response of bolted T-stubs. Two main modifications are introduced. The first concerns the position of the resultant prying force, which in most closed-form models is assumed to act at a fixed location, usually at the tip of the flange plate. However, this assumption is not consistent with the actual behaviour as demonstrated by past works. Particularly, Couchaux et al. [13] showed that the contact force cannot, in general, be reduced to a fixed force applied at the plate edge, since the contact area evolves with loading and the position of the resultant depends on the relative stiffness of the flange and the bolt system (Fig. 10). In Couchaux et al. [13] model, the flange is divided into a non-contact region, described by classical beam theory, and a contact region, described by an enriched beam formulation accounting for transverse deformation effects.

On this basis, they derived first a closed-form expression for the ratio between bending moment and shear force at the transition between the contact and non-contact regions, and then a simplified approximation for practical use. Couchaux et al. [13] work has also lead to a simplified direct analytical expression for the distance n of the resultant contact force from the bolt axis, namely:

$$n = \min[(2e + \zeta)/3; \zeta + 0.74t] \quad (8)$$

where $\zeta = e \sqrt{\frac{\alpha_{R,0}}{\alpha_R}} \leq e$ with $\alpha_R = 4 \left(\frac{m}{t}\right)^3 \frac{A_{res}}{L_b b_{eff}}$, $\alpha_{R,0} = \frac{e/m+1}{(e/m)^3}$ where L_b is the bolt length. In physical terms, this formulation reflects the fact that the resultant contact force is located closer to the bolt axis in more ductile configurations and progressively moves towards the plate edge as the flange becomes stiffer and the behaviour approaches the classical prying-force idealisation. Couchaux et al. [13] also showed in their parametric study that, for thin flanges, the prying force may be located well inside the flange span, whereas for thick flanges it approaches the plate edge. Adopting this formulation in the present model leads to significant modifications both in terms of classification and in terms of predictive ability of the model. Indeed, since n enters directly the definition of $\lambda = n/m$, a revised evaluation of the prying force position modifies the expressions of the resistance, the value of ψ , and, consequently, the boundaries of β separating the different collapse mechanisms. In particular, the range associated with MEC-1 becomes narrower



Fig. 10. Proposal for identifying the location of contact/prying forces [13]: a) Mode 1; b) Mode 2.

than in the classical formulation based on contact at the plate tip, and the transition between the flange-dominated and bolt-dominated regimes is described more realistically.

The second major modification which is proposed in this work concerns the introduction of an additional transition mechanism between MEC-2 and MEC-3. The model by Lyu et al. [14] already improved the classical EC3 framework by introducing the intermediate mechanism MEC-1.2, thus recognising that the transition between flange-dominated and mixed response is not abrupt. However, both experimental evidence and mechanical considerations indicate that a gradual transition also occurs between MEC-2 and MEC-3 [15,16]. In particular, in Lyu et al. it [14] has been already demonstrated that the loss of contact may take place for values of β lower than 2, meaning that the disappearance of contact forces may precede the fully bolt-governed response traditionally associated with MEC-3. This observation makes it necessary to introduce a further transition regime, which is here denoted as MEC-2.3.

Mechanically, MEC-2.3 is characterised by bolt rupture in the

absence of contact forces, while a plastic hinge is still allowed to develop at the plate-to-web junction. It therefore represents an intermediate configuration between MEC-2, in which bolt rupture is accompanied by contact interaction and significant flange plasticity, and MEC-3, in which the response is essentially governed by bolt failure alone. The introduction of MEC-2.3 is particularly important because an incorrect classification of these configurations as MEC-2 would not only affect the prediction of the ultimate resistance, but would also lead to a significant overestimation of the deformation capacity. Accordingly, the proposed classification extends the four-mechanism framework of Lyu et al. to a five-mechanism scheme (Fig. 11, which includes both MEC-1.2 and the newly introduced MEC-2.3). The analytical derivation of the corresponding boundary values of β , together with the associated expressions for ψ , is hereinafter presented.

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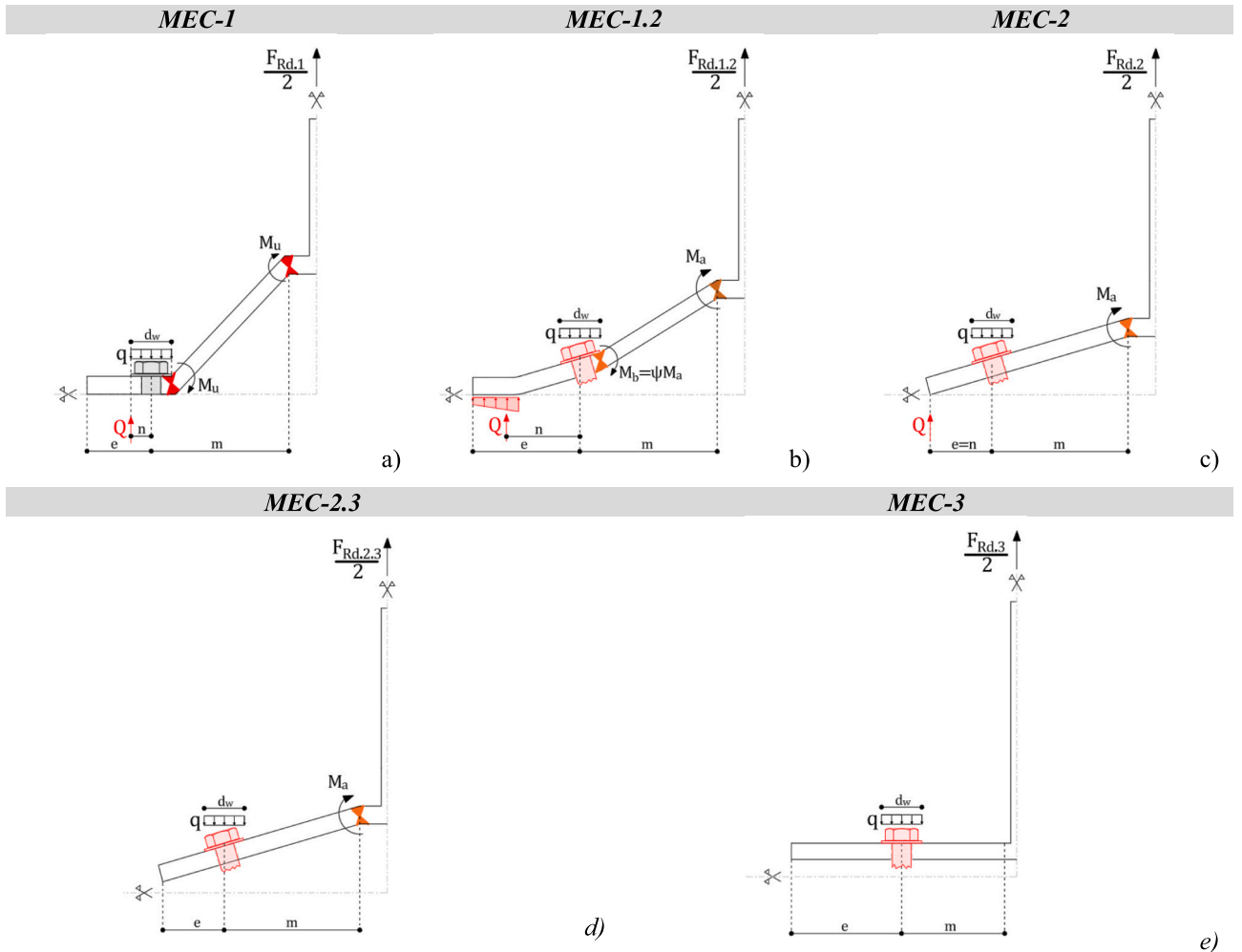


Fig. 11. 5 modes classification approach.

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In MEC-2, bolt rupture occurs together with the formation of a plastic hinge at the flange-to-web junction, while contact forces are still present. The transition to MEC-3 is identified by the loss of these contact forces. The results of the wide parametric study presented in next section show that at the transition from MEC-2 to MEC-3 contact forces vanish while the bending moment at the flange-to-web junction is still higher than the fully plastic moment. This means that, at the onset of this transitional failure mode (here called MEC-2.3), the bolt already governs the failure, but a certain degree of flange plasticity is still maintained. On this basis, the boundary between MEC-2 and MEC-2.3 may be determined by writing a very simple equilibrium of a cantilever beam loaded by the bolt resistance, together with a bending moment at the flange hinge representative of the transition condition. As suggested by the parametric analyses, the moment at the flange-to-web junction at this transition can be reasonably assumed equal to approximately 1.25 times the plastic moment, that is $M_a \approx 1.25M_{pl}$. By equating the bolt force at failure to the corresponding flange action under this condition, the transition value of β is obtained as follows:

$$M_a \approx 1.25M_{pl} = B_{rd}m \rightarrow 1.25 \frac{2M_{pl}}{B_{rd}m} = 2 \rightarrow \beta_{2.3} = \frac{2}{1.25} = 1.6 \quad (9)$$

Table 4

Proposed range of β , ψ , F_u and k -factor.

Range of β		ψ	Ultimate resistance F_u	k -factor ($\delta_p = \delta_{b,u} \frac{\lambda + k}{\lambda}$)
MEC-1	$\left[0; \frac{2\lambda\xi}{1+2\lambda}\right]$	1	$\frac{4M_u}{m}$ or $\frac{(32n-2d_w)}{8mn-(m+n)d_w}M_u$	$k_1 = \left(\frac{Cm^2}{2t\delta_{b,u}} - 1\right)\lambda$
MEC-1.2	$\left[\frac{2\lambda\xi}{1+2\lambda}; \frac{1}{\eta} \left(\sqrt{1 + \frac{4\lambda\eta}{1+2\lambda}} - 1\right)\right]$	$\frac{\lambda(\beta_3 - \beta)}{\left(\frac{1+2\lambda}{\xi} - \lambda\right)\beta}$	$2B_{rd} \frac{\lambda(1+\psi)}{\lambda(1+\psi) + \psi}$	$k_{1.2} = k_1 + (1-k_1) \left(\frac{\beta - \beta_1}{\beta_{1.2} - \beta_1}\right)^{6 \cdot \left(\frac{Cm^2}{2t\delta_{b,u}}\right)^{-1.5}}$
MEC-2	$\left[\frac{1}{\eta} \left(\sqrt{1 + \frac{4\lambda\eta}{1+2\lambda}} - 1\right); 1.6\right]$			$k_2 = 1$
MEC-2.3	$[1.6; 2]$	0	$2B_{rd}$	$k_{2.3} = \frac{(\beta\beta_3)^2}{(\beta_{2.3}\beta_3)^2} = 6.25(\beta_2)^2$
MEC-3	$[2; +\infty]$			$k_3 = 0$

* An alternative formulation for the ultimate strength in MEC-1.2 and MEC-2 can also be derived by accounting for the influence of the washer diameter [14]. However, such a formulation is rather cumbersome for practical design applications and does not lead to a significant improvement in the prediction of resistance. For this reason, it has not been adopted in the present modelling approach.

Therefore, the limit $\beta_{2.3} = 1.6$ is adopted in this work to separate MEC-2 from MEC-2.3. From a mechanical point of view, this threshold marks the configuration in which bolt failure still occurs in the presence of residual flange plasticity, but after the disappearance of contact forces. The ranges of variation of the parameter β , together with the corresponding expressions for the coefficient ψ and the ultimate resistance F_u , are reported in Table 4. It is noteworthy that the value $\beta_{2.3} = 1.6$ identified from the parametric analyses is consistent with the transition limit proposed by [17]. Nevertheless, this threshold should not be generalized beyond the specific field of application or range of data considered in this work (e.g. not to be extended to high strength steels) as this is not the result of a purely theoretical analysis.

Fig. 12 presents the ψ - β relationship predicted by the proposed model and compares it with the corresponding curves obtained from the models by Lyu et al., Faella et al., and EC3 part.1–8. The figure highlights the influence of the adopted failure-mode classification on the evolution of ψ , which may be interpreted as a measure of the level of flange plasticity. Mechanically, MEC-1 and MEC-1.2 are characterised by the formation of two plastic hinges, while MEC-2, and MEC-2.3 involve only one plastic hinge. Furthermore, contact forces remain active in MEC-1, MEC-1.2 and MEC-2, whereas they vanish in MEC-2.3 and MEC-3. With respect to Lyu et al. formulation, the proposed model predicts a narrower MEC-1 range, due to the revised evaluation of λ .

Because λ is defined through the actual position of the resultant contact force, rather than by assuming contact at the plate tip, its value generally decreases, and this affects all the β -limits separating the various mechanisms. As a result, not only the boundary of MEC-1, but also those of MEC-1.2 and of the following mechanisms are shifted.

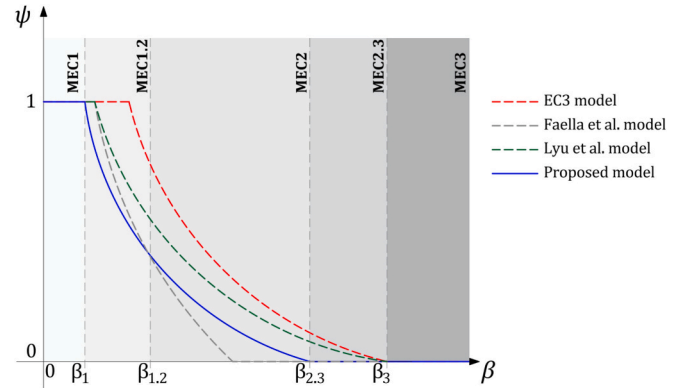


Fig. 12. ψ vs β in the framework of a five-modes classification.

3.2. Prediction of the ultimate resistance

The ultimate resistance associated with the different collapse mechanisms is evaluated by means of the expressions reported in Table 4. In this respect, the proposed formulation follows the same mechanical framework adopted by Faella et al. [8] and later by Lyu et al. [14], with the main difference lying not in the equilibrium equations themselves, but in the revised failure-mode classification introduced in the previous section. Since the governing mechanism is identified through different threshold values of β , and since the parameter λ is redefined through the actual position of the resultant contact force, the domains of application of the various resistance expressions differ from those of the Faella et al. [8] and Lyu et al. [14] models. In particular, the introduction of the additional transition mechanism MEC-2.3 and the revised ranges of β leads to a corresponding update of the evaluated T-stub resistance.

From a mechanical viewpoint, the prediction of the ultimate resistance beyond the geometric properties depends mainly on three quantities: the ultimate tensile resistance of the bolts (B_{Rd}), the ultimate bending resistance of the flange plate (M_u), and the coefficient ψ , which reflects the collapse mechanism through the ratio between the bending moment acting at the bolt line and that acting at the plate-to-web junction. The bolt resistance is evaluated in the conventional way and remains unchanged with respect to the previous formulations. By contrast, the value of ψ has already been revised in the previous section, consistently with the new classification of the collapse mechanisms. Therefore, the main issue discussed herein concerns the evaluation of the ultimate bending resistance of the flange plate.

A rigorous formulation for this purpose was originally proposed by Faella et al., who derived the ultimate bending moment directly from the material stress-strain response by integrating the nonlinear stress distribution across the plate thickness and assuming that failure is reached when the ultimate strain develops in the most stressed fibre of the section. This approach is mechanically consistent and provides an accurate estimate of the ultimate plate resistance. It was later adopted in substantially the same form by the more recent full-range formulations. However, although Faella et al. also provided a closed-form expression for the ultimate bending moment, this expression remains relatively cumbersome, since it depends on several parameters associated with the quadrilinear material law (Fig. 13) and therefore is not particularly convenient for simplified analytical applications.

$$M_{u,F} = \frac{M_y}{2} \left[3 - \left(\frac{\epsilon_y}{\epsilon_u} \right)^2 \right] + \frac{1}{2} \frac{E_h}{E} \left(\frac{\epsilon_u - \epsilon_y}{\epsilon_y} \right) \left(1 - \frac{\epsilon_h}{\epsilon_u} \right) \left(2 + \frac{\epsilon_h}{\epsilon_u} \right) - \frac{1}{2} \frac{E_h - E_u}{E} \left(\frac{\epsilon_u - \epsilon_m}{\epsilon_y} \right) \left(1 - \frac{\epsilon_m}{\epsilon_u} \right) \left(2 + \frac{\epsilon_m}{\epsilon} \right) \quad (10)$$

At the opposite end, a much simpler approximation is obtained if the ultimate bending moment is evaluated according to Jaspart [6], that is, by directly replacing the yield stress with the ultimate stress in the

conventional plastic expression.

$$M_{u,J} = \frac{bt^2}{4} f_u \quad (11)$$

This approximation is attractive because of its simplicity and might be still used for the prediction of resistance itself reaching sufficient accuracy, but it is too crude for the purposes of the present work. The main limitation is related to the consequence that such a simplified estimate has on the subsequent evaluation of the deformation capacity. Indeed, as the moment-curvature response of a plate becomes progressively flatter in the ultimate strain range, even a limited overestimation of the ultimate moment will translate into a significant overestimation of the available plastic rotation and, hence, of the plastic hinge rotation ultimate capacity.

For this reason, the present model adopts an intermediate solution based on the formulation proposed by Lemonis and Gantes [32]. In their model, the flange material is represented by a bilinear stress-strain law (Fig. 13) with strain hardening, and the ultimate bending resistance is obtained by integrating the stress distribution across the plate thickness under the assumption that fracture occurs when the extreme fibre reaches the ultimate strain. The corresponding closed-form expression is:

$$M_u = \frac{bt^2}{12} \left(3(E - E_T)\epsilon_y + 2E_T\epsilon_u - \frac{(E - E_T)\epsilon_y^3}{\epsilon_u^2} \right) \quad (12)$$

which, for structural steels such that $\epsilon_u \gg \epsilon_y$, simplifies to:

$$M_u = \frac{bt^2}{4} \cdot \frac{(f_y + 2f_u)}{3} \quad (13)$$

or, equivalently, considering that for normal steel grades $f_y/f_u \approx 0.67$:

$$M_u \approx \frac{bt^2}{4} \cdot \frac{(2.67f_u)}{3} \approx \frac{bt^2}{4} (0.9f_u) \quad (14)$$

This formulation represents a convenient compromise between accuracy and simplicity. On the one hand, it retains the mechanical meaning of the ultimate state by linking M_u to the attainment of the ultimate strain in the extreme fibre. On the other hand, it avoids the algebraic complexity of the rigorous expression proposed by Faella et al. [8]. For the purposes of the present work, this is particularly advantageous, since the objective is not to reproduce the full moment-curvature response of the flange, but rather to obtain a reliable and compact estimate of its ultimate bending resistance to be used within a closed-form mechanical model. It is noteworthy that this simplified formulation provides an accurate approximation of the ultimate bending resistance while avoiding the complexity of the rigorous expression proposed by Faella et al. [8]. For instance, referring to all the 58 experimental cases described in Section 2.3, considering the actual material properties (true stress-true strain), the average value of the ratio $M_{u,F}/M_u$ is equal to 0.99, with a CoV of 0.01, confirming the equivalence of this simplified

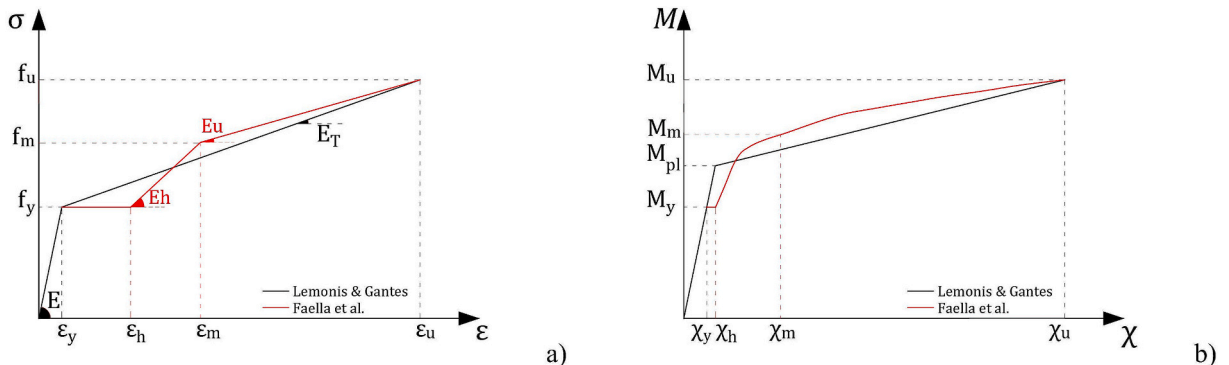


Fig. 13. Material modelling of the T-stub flange: a) Constitutive law; b) Moment-curvature relationship.

approach.

In conclusion, the proposal to evaluate the ultimate resistance of a bolted T-stub is based on the same equilibrium structure adopted by Faella et al. and Lyu et al., but with three essential modifications: a revised classification of the collapse mechanisms (see Section 3.1), a corresponding redefinition of the parameter ψ (see Section 3.1), and a simplified yet mechanically consistent evaluation of the ultimate bending moment based on the formulation by Lemonis and Gantes. The bolt ultimate resistance is kept unchanged, while the new classification and the revised value of λ affect the ranges of β and therefore the corresponding resistance equations reported in Table 4.

Fig. 14 shows the evolution of the normalized ultimate resistance $F/(2B_{Rd})$ as a function of β , for fixed material and geometric properties. Since β controls the governing collapse mechanism, its variation affects both the value of ψ and the resistance expression to be used. The figure highlights two main aspects. First, the ultimate resistance predicted by the proposed model is obviously always higher than the plastic resistance provided by EC3 part 1.8, because strain hardening of the flange is explicitly taken into account. Second, the corresponding mechanism classification differs significantly from that of the Eurocode. In particular, the MEC-1 domain is shifted to lower values of β and becomes much narrower, while the transition from flange-dominated to bolt-governed behaviour is more gradual. This reflects the fact that, once the actual position of the contact force is introduced in the evaluation of λ , both the failure-mode boundaries and the associated resistance levels are modified. The proposed model therefore captures not only the increase in resistance beyond the plastic level, but also the progressive nature of the transition between collapse mechanisms.

3.3. Prediction of the deformation capacity

The prediction of T-stub deformation capacity is not addressed in EC3 part 1–8. Most available mechanical models estimate the deformation capacity from the plastic rotations of the flange and the deformation capacity of the bolts, but, as previously mentioned, in many cases the ultimate displacement is obtained through incremental or iterative procedures that are too complex for practical use. Among the few formulations providing closed-form expressions, the models proposed by Faella et al. and Beg et al. are the most relevant, although each of them is accurate only for specific collapse mechanisms.

The formulation by Faella et al. provides a reliable estimate of the displacement capacity for MEC-1, where collapse is governed by flange yielding. In this case, failure is assumed to occur when the ultimate rotation capacity is reached at the plastic hinges forming at the flange-to-web connection and at the bolt line, i.e. $M_a = M_b = M_u$. The corresponding plastic rotations are obtained by integrating the moment-curvature relationship of the flange plate up to the attainment of the

ultimate material strain. However, the same approach becomes inaccurate for MEC-2, because it assumes the simultaneous attainment of the ultimate rotation at the flange-to-web connection ($M_a = M_u$) and bolt failure. As previously discussed, this condition is not representative of the actual response, since at collapse $M_a < M_u$ in bolt-governed mechanisms. As a result, the model significantly overestimates the plastic hinge rotation and, consequently, the deformation capacity.

The approach by Beg et al. is instead attractive because of its simplicity. In that model, the ultimate displacement is expressed as the sum of a contribution due to flange plastic rotation and a contribution due to bolt elongation. This rationale is particularly suitable for bolt-governed mechanisms, but the original formulation is based on highly simplified assumptions concerning the ultimate material strain, the hinge length, and the relation between bolt uplift and flange rotation. These approximations limit its predictive accuracy, especially in the transition between adjacent mechanisms.

Starting from these considerations, the present formulation combines the strengths of the two approaches. For MEC-1, the ductility prediction follows the mechanical framework proposed by Faella et al., which has been shown to be accurate for flange-governed collapse. For MEC-2 and MEC-3, where failure is governed by bolt rupture, the proposed model adopts a formulation consistent with the rationale of Beg et al. The main contribution of the present work concerns the transition mechanisms MEC-1.2 and MEC-2.3, for which dedicated expressions are introduced. On this basis, the displacement capacity of the T-stub can be written in a general form as the sum of elastic and plastic:

$$\delta = \delta_e + \delta_p = \frac{F_u}{K_e} + (\delta_b + \vartheta_p m) \quad (15)$$

where δ_p is the plastic rotation of the hinge at the flange-to-web attachment, δ_b is the plastic bolt deformation, and F_u/K_e is the elastic deformation of the T-stub, with K_e is evaluated according to the component-based stiffness model of EC3 part 1.8. In principle, δ_p could be obtained by integrating the curvature distribution corresponding to the bending moment acting in the flange at collapse, M_a . However, such a procedure is cumbersome and not well suited to a closed form analytical solution. Moreover, the moment-rotation response of plastic hinges becomes rather flat in the ultimate range, so that even small errors in the estimation of M_a may lead to large errors in the calculated rotation. For this reason, it is considered more rational to relate the flange plastic rotation directly to the bolt ultimate deformation, as originally suggested by Beg et al., through the following expression:

$$\vartheta_p = k \frac{\delta_{b,u}}{n} \quad (16)$$

where k is a dimensionless parameter governing the distribution of deformation between bolt elongation and flange plastic rotation. Starting from this assumption, it is easy to verify that for all the failure modes involving the bolt collapse (MEC-1.2, MEC-2, MEC-2.3 and MEC-3) the T-stub plastic deformation capacity becomes:

$$\delta_p = \left(\delta_{b,u} + k \frac{m}{n} \delta_{b,u} \right) = \delta_{b,u} \left(1 + \frac{k}{\lambda} \right) = \delta_{b,u} \frac{\lambda + k}{\lambda} \quad (17)$$

The value of k depends on the governing collapse mechanism and is defined in the following for each of the five mechanisms considered in the present work.

3.3.1. MEC-1

For MEC-1, no plastic deformation develops in the bolt, so that $\delta_b = 0$, while the plastic rotation at the flange-to-web connection reaches its ultimate value, i.e. $\vartheta_p = \vartheta_u$ (Fig. 15a). In this case, the formulation by Faella et al. can be adopted, leading to the following simplified closed-form expression for ϑ_u :

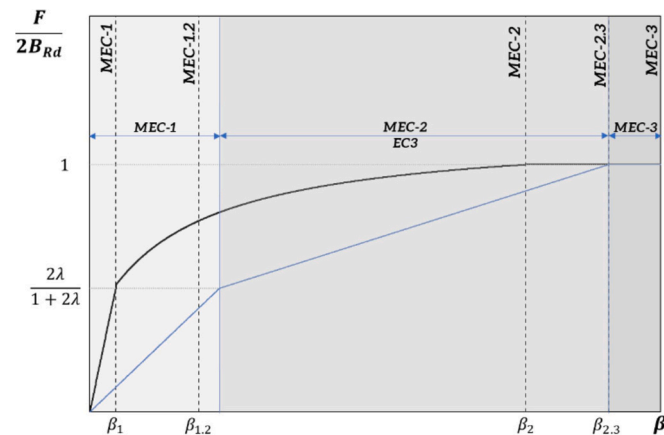


Fig. 14. Normalized ultimate and plastic resistance vs. β .

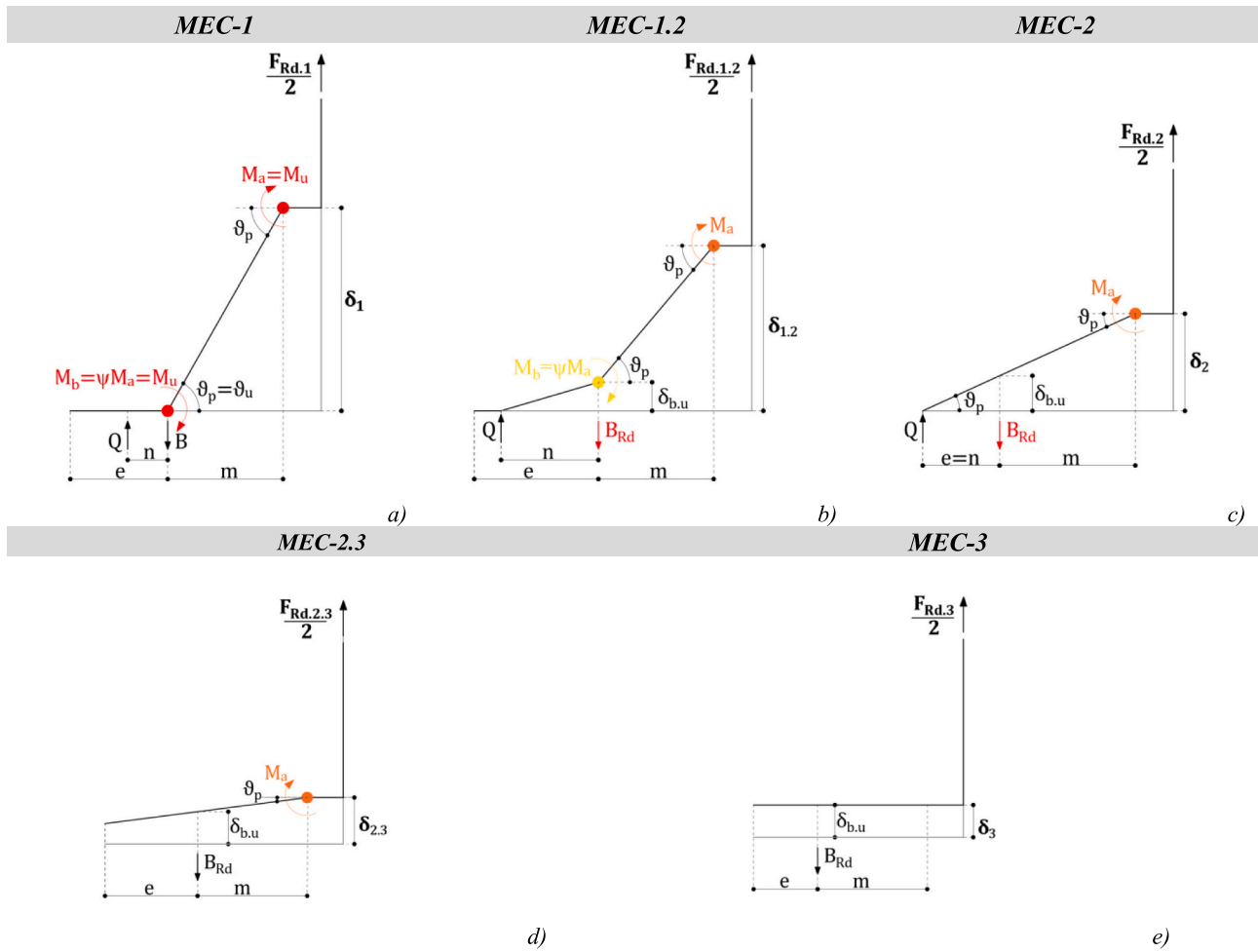


Fig. 15. Proposed kinematic mechanisms at failure.

$$\vartheta_u = \frac{Cm}{2t} \quad (18)$$

where the parameter C depends only on the material properties and can be expressed as:

$$C = 2\varepsilon_y \left[\frac{e_u}{\varepsilon_y} - \frac{1}{2M_u/M_y} (1 + C_1 + C_2 + C_3) \right] \quad (19)$$

with:

$$C_1 = 3 \frac{\chi_h}{\chi_y} + \frac{\chi_y}{\chi_h} - 4 \quad (20)$$

$$C_2 = \frac{\chi_h (3\chi_m^2 + \chi_y^2) - \chi_m \chi_y^2 - 3\chi_h^2 \chi_m}{\chi_y \chi_h \chi_m} + \frac{E_h}{E} \frac{(\chi_m \chi_h)^3}{\chi_y^2 \chi_m} \quad (21)$$

$$C_3 = \frac{\chi_m (3\chi_u^2 + \chi_y^2) - \chi_u \chi_y^2 - 3\chi_m^2 \chi_u}{\chi_y \chi_m \chi_u} - \frac{E_h - E_u}{E} \frac{(\chi_u \chi_m)^3}{\chi_y^2 \chi_u} + \frac{E_h}{E} \frac{\chi_h^3 (\chi_u - \chi_m) - 3\chi_h \chi_m \chi_u (\chi_u - \chi_m) + \chi_m \chi_u (\chi_u^2 - \chi_m^2)}{\chi_y^2 \chi_m \chi_u} \quad (22)$$

Once the stress-strain law has been defined for a steel grade, the value of C can be derived directly from the previous expressions where $\chi = 2\varepsilon/t$. Therefore, the plastic deformation capacity of a T-stub is evaluated as:

$$\delta_p = \vartheta_u m = \frac{Cm^2}{2t} \quad (23)$$

It is worth observing that, eventually, previous Eq. (17) might be still used adopting for MEC-1 a fictitious value of $k = k_1$:

$$k_1 = \left(\frac{Cm^2}{2t \cdot \delta_{b,u}} - 1 \right) \lambda \equiv \left(\frac{\vartheta_u m}{\delta_{b,u}} - 1 \right) \lambda \quad (24)$$

3.3.2. MEC-2

In the case of MEC-2, according to the kinematic mechanism shown in Fig. 15c, the bending moment at the flange-to-web connection satisfies $M_{pl} < M_a < M_u$, so that the plastic rotation at this section is smaller than the ultimate one, namely $\vartheta_p < \vartheta_u$. At the same time, since $M_b < M_{pl}$, no plastic rotation develops at the bolt line. Because collapse is governed by bolt rupture, the bolt deformation reaches its ultimate value $\delta_{b,u}$, thus the plastic rotation at the flange-to-web connection can be approximated as:

$$\vartheta_p = \frac{\delta_{b,u}}{n} \quad (25)$$

Accordingly, the parameter $k = k_2 = 1$, leading to the following equation for the displacement capacity of MEC-2:

$$\delta_p = \delta_p = \frac{\delta_{b,u}}{n} (m + n) = \delta_{b,u} \left(1 + \frac{1}{\lambda} \right) = \delta_{b,u} \frac{\lambda + 1}{\lambda} \quad (26)$$

3.3.3. MEC-1.2

In MEC-1.2, collapse is governed by bolt rupture, but plastic hinges still develop both at the flange-to-web junction and at the bolt line. Therefore, $\delta_b = \delta_{b,u}$, while $\vartheta_p < \vartheta_u$ (Fig. 15b). In this case, the evaluation of k is not straightforward and cannot be derived easily with a purely mechanical model. A specific regression expression is therefore introduced as the result of a parametric study developed in this work based on the variation of a wide range of geometrical and mechanical parameters. The results of this parametric study are presented in the next section, while the expression for the transition from MEC-1 to MEC-2 has been modelled adopting a specific power expression:

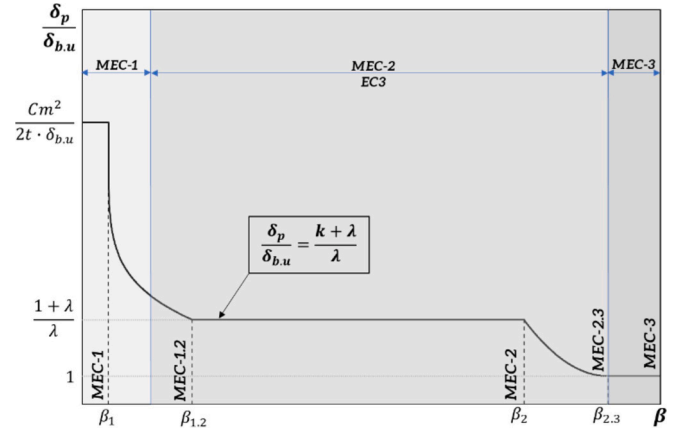


Fig. 16. Normalized Plastic deformation capacity vs β .

$$k_{1.2} = k_1 + (1 - k_1) \left(\frac{\beta - \beta_1}{\beta_{1.2} - \beta_1} \right)^6 \left(\frac{Cm^2}{2t \cdot \delta_{b,u}} \right)^{-1.5} \quad (27)$$

This expression is continuous over the whole transition range. In particular, it returns the value of k_1 at $\beta = \beta_1$ and tends to one at $\beta = \beta_{1.2}$, consistently with the transition towards MEC-2.

3.3.4. MEC-3

In MEC-3, no plastic hinge develops in the flange and the collapse is governed exclusively by bolt rupture. Consequently, in this case the same expressions can be adopted with $k = k_3 = 0$ (Fig. 15e). Therefore, the displacement capacity reduces to the sum of the elastic deformation of the flange and the bolt ultimate elongation. The deformation capacity of the flange can be directly evaluated considering the plate as a cantilever loaded by the bolt force B_{Rd} , leading to the following expression:

$$\delta_3 = \frac{4B_{Rd}m^3}{Eb_{eff}t^3} + \delta_{b,u} \quad (28)$$

3.3.5. MEC-2.3

MEC-2.3 represents the transition between the classical MEC-2 and the purely bolt-governed mechanism MEC-3. In this configuration, collapse is still governed by bolt rupture, while a limited plastic rotation may still develop at the flange-to-web connection. However, unlike MEC-2, no contact forces are present. As a result, the contribution of flange plasticity to the overall displacement capacity is smaller than in MEC-2 and progressively vanishes as the response approaches MEC-3 (Fig. 15d).

Similarly to MEC-1.2, the parameter k is defined through a dedicated regression law, calibrated to reproduce the gradual transition between the adjacent mechanisms. Based on the available data, it can be seen that the transition follows a quite simple quadratic expression, governed by the following law:

$$k_{2.3} = \frac{(\beta\beta_3)^2}{(\beta_{2.3}\beta_3)^2} = 6.25(\beta_2)^2 \quad (29)$$

This expression is continuous over the whole transition range. In particular, it reproduces the value corresponding to MEC-2 at $\beta = \beta_{2.3}$, while tending to zero as β approaches β_3 , consistently with the transition towards MEC-3. A similar consideration applies here as for MEC-1.2. Accordingly, the displacement capacity in the MEC-2.3 range is expressed as:

$$\delta_{2.3} = \frac{4B_{Rd}m^3}{Eb_{eff}t^3} + \delta_{b,u} \frac{\lambda + k_{2.3}}{\lambda} \quad (30)$$

Based on the application of previous expressions Fig. 16 shows the

variation of the normalized plastic displacement $\delta_p/\delta_{b,u}$ as a function of β , according to the general expression $\delta_p = \delta_{b,u}(\lambda + k)/\lambda$. The figure highlights the difference between the Eurocode 3 classification, based on plastic mechanisms, and the ultimate-response-based classification proposed herein. In particular, the transition mechanisms MEC-1.2 and MEC-2.3 play a crucial role in the prediction of deformation capacity. If the MEC-1 expression, essentially based on the formulation by Faella et al., were used throughout the whole range conventionally associated with flange-dominated behaviour, the displacement capacity would be markedly overestimated in the MEC-1.2 interval. Conversely, adopting directly the MEC-2 formulation in the same range would lead to an underestimation. An analogous situation occurs in the transition from MEC-2 to MEC-3, where the direct use of the MEC-2 and MEC-3 expressions would respectively overestimate and underestimate the actual deformation capacity. The proposed formulation overcomes this limitation by combining the Faella-based description for MEC-1 with the Beg-type approach for MEC-2 and MEC-3, and by introducing dedicated transition laws for MEC-1.2 and MEC-2.3. As a result, the predicted deformation capacity varies continuously with β and more closely reflects the actual mechanical response of the T-stub.

It is noteworthy that, on the basis of the previous considerations, the deformation capacity of the T-stub can be evaluated by adopting a unified formulation in which the plastic displacement is expressed as a function of the parameter k , namely:

$$\delta_p = \delta_{b,u} \frac{\lambda + k}{\lambda} \quad (31)$$

Whereas the value of k depends on the governing failure mechanism (Table 4).

4. Model verification and validation

4.1. Parametric study and model verification

In order to verify the accuracy of the proposed formulation, a comprehensive parametric study was carried out by applying the advanced mechanical model proposed by Francavilla et al. [12] to a large number of cases.

As discussed in the introductory section, the mechanical model proposed by Francavilla et al. [12] has been shown to provide an accurate prediction of the full-range response of bolted T-stubs, since it accounts for the main nonlinear mechanisms governing collapse, including material nonlinearity in the flange plate and in the bolts, contact interaction, and large-displacement effects. The model is based on a mechanical representation in which the flange plate is described by means of nonlinear plastic hinges, while the bolts are represented through axial and shear springs. In the present study, this model is adopted as a reference solution for the evaluation of ultimate resistance, deformation capacity, and governing failure mode. However, because the response is obtained through an incremental step-by-step procedure

based on equilibrium and compatibility conditions, the model is too complex for direct practical use in closed-form design calculations, while being particularly suitable for the parametric analyses developed herein.

The parametric analyses were carried out by considering a wide range of geometric and mechanical parameters, in order to investigate their influence on the ultimate resistance, deformation capacity, and governing failure mode of bolted T-stubs. In particular, the following non-dimensional parameters were varied within the ranges:

- $4 \leq m/t \leq 10$
- $0.3 \leq e/m \leq 1.25$;
- $1.5 \leq b_{eff}/m \leq 4.5$;
- $2 \leq m/d_b \leq 5$;
- $2.25 \leq f_{ub}/f_y \leq 4.25$

By combining all parameters, a total of 138 cases were identified and used for further analysis. The generated data served two main purposes: i) to calibrate the parameter k in the transition mechanisms MEC-1.2 (Eq. (27)) and MEC-2.3 (Eq. (29)), for which a direct closed-form mechanical definition is not straightforward; and ii) to assess the accuracy of the proposed formulation in predicting both ultimate resistance and deformation capacity over a broad range of cases. According to the classification criteria proposed in this work, 16 cases correspond to MEC-1, 42 to MEC-1.2, 39 to MEC-2, 15 to MEC-2.3, and 26 to MEC-3.

4.2. Model accuracy on synthetic data based on mechanical modelling

To assess the accuracy of the proposed closed-form expressions in predicting both ultimate resistance and deformation capacity, they were applied to the 138 parametric cases generated by means of the reference mechanical model. The comparison was carried out by considering, for each case, the failure mode identified through the proposed β -based classification and the corresponding predictive expressions reported in Table 4. Therefore, before discussing the predictive accuracy of the proposed closed-form expressions in terms of ultimate resistance and deformation capacity, it is useful to preliminarily assess the ability of the proposed procedure to correctly identify the governing failure mechanism. Since the analytical expressions are mechanism-dependent, the reliability of the classification stage represents a necessary prerequisite

Table 5
Model performance.

	MEC-1	MEC-1.2	MEC-2	MEC-2.3	MEC-3	All analysed cases
$F_u/F_{u,par}$	1.00	1.16	1.10	1.01	1.00	1.08
CoV	0.10	0.10	0.14	0.01	0.00	0.12
δ/δ_{par}	1.02	1.00	1.00	0.87	0.96	0.98
CoV	0.11	0.18	0.13	0.10	0.04	0.14

		<i>Predicted</i>				
		MEC 1	MEC 1-2	MEC 2	MEC 2-3	MEC 3
<i>Actual</i>	MEC 1	14	0	0	0	0
	MEC 1-2	1	29	1	0	0
	MEC 2	1	13	38	0	0
	MEC 2-3	0	0	0	15	0
	MEC 3	0	0	0	0	26

Fig. 17. Confusion matrix comparing the mechanism classification from the reference mechanical model and the proposed β -based 5 modes classification model for the 138 synthetic cases.

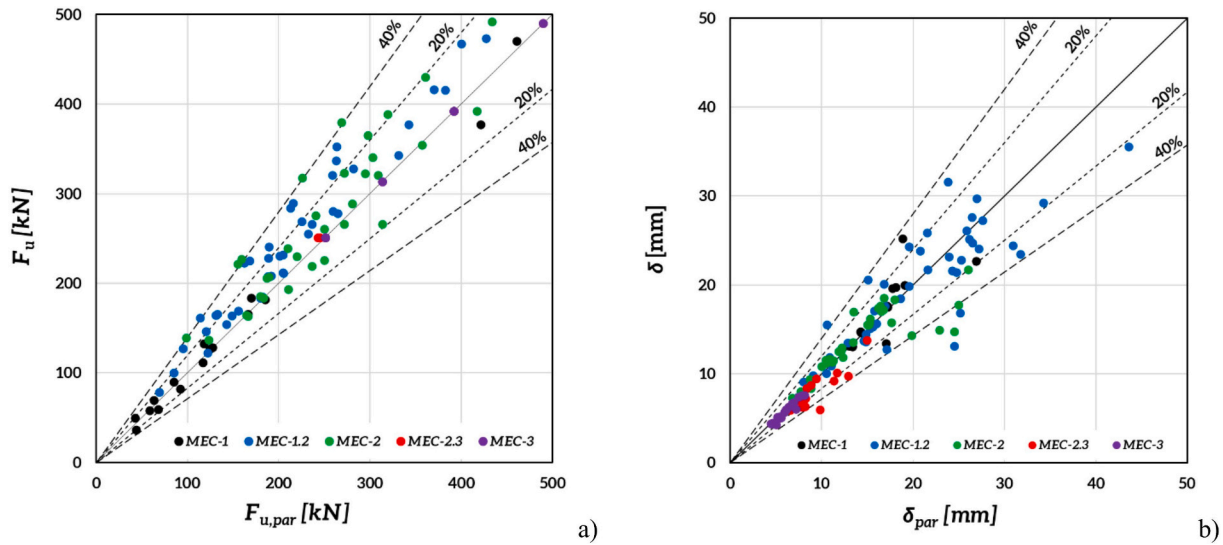


Fig. 18. Comparison between parametric analysis results and previsions: a) ultimate resistance; b) ultimate displacement.

for the consistency of the overall simplified model. To this end, the failure mode predicted by the proposed β -based classification was compared, on a case-by-case basis, with the mechanism obtained from the reference mechanical model for the full set of 138 synthetic parametric analyses. The mechanism identified by the reference model was assumed as ground truth, while the output of the proposed classification was treated as the model prediction. This comparison allows one to assess whether the simplified formulation is able to reproduce not only the global response quantities, but also the underlying mechanical interpretation of the structural behaviour.

Overall, the agreement between the two classifications is very good. Out of the 138 analysed cases, 122 were assigned to the correct mechanism, corresponding to an exact-match accuracy of 88.4%. The results are summarised in the confusion matrix reported in Fig. 17, which highlights a generally strong consistency between the proposed classification and the reference mechanical model across the full dataset. A mechanism-by-mechanism analysis shows that the agreement is excellent for the most clearly distinguishable regimes. In particular, perfect agreement was obtained for MEC-1, MEC-2.3 and MEC-3, while MEC-1.2 was also identified with very high accuracy. More specifically, the classification accuracy was 100% for MEC-1, 93.5% for MEC-1.2, 73.1% for MEC-2, 100% for MEC-2.3, and 100% for MEC-3. These results indicate that the proposed procedure is fully consistent with the reference model for the extreme and transition mechanisms, while the main source of uncertainty is concentrated in the MEC-2 domain. A closer examination of the confusion matrix shows that the observed discrepancies are not randomly distributed. On the contrary, almost all misclassifications involve cases identified by the reference model as MEC-2 and classified by the proposed approach as MEC-1.2. This result is mechanically meaningful, since MEC-2 corresponds to an intermediate regime and is therefore more sensitive to variations in the parameters governing the position of the contact force and the redistribution of internal actions. The fact that the misclassifications are concentrated between adjacent mechanisms, rather than involving distant regimes, suggests that the proposed formulation captures the mechanical transition correctly and that the residual disagreement is mainly associated with borderline cases.

With regard to ultimate resistance, the comparison shows a good overall agreement between the proposed formulation and the results of the parametric analyses. Over the entire dataset, the mean value of the ratio $F_u/F_{u,par}$ is 1.08, with a coefficient of variation equal to 0.12. As shown in Table 5 and Fig. 18a, the proposed expressions are generally able to reproduce the ultimate resistance predicted by the reference

model with satisfactory accuracy over the full range of analysed configurations.

A more detailed examination indicates that the agreement is particularly satisfactory for the classical mechanisms and remains satisfactory also within the transition ranges. As expected, the largest deviations are observed for MEC-1.2, which is also the mechanism most sensitive to the evaluation of the contact force position. In this range, the prediction of the ultimate resistance is affected by the simplified estimation of the parameter η , whose determination is inherently non-trivial. Nevertheless, even in this case, the proposed formulation provides a reasonably accurate representation of the numerical response. By contrast, the agreement is very good for MEC-2.3 and exact for MEC-3, for which the proposed formulation substantially coincides with the reference mechanical model. A similarly satisfactory trend is observed for the prediction of the ultimate displacement. Over the entire dataset, the mean ratio δ/δ_{par} is 0.98, with a coefficient of variation equal to 0.14. This confirms that the proposed formulation is also able to reproduce the deformation capacity predicted by the reference model with good overall accuracy. As reported in Table 5 and Fig. 18b, the proposed expressions perform very well for the classical mechanisms and maintain a satisfactory level of accuracy also in the transition ranges. Having verified the consistency of the proposed expressions within the parametric framework, their applicability is further assessed in the following section through comparison with experimental results.

4.3. Model validation against test data

To assess the applicability of the proposed predictive formulations for classification, ultimate strength and ultimate displacement, it was applied to the experimental database introduced in Section 2.3 and summarised in Table 2. The complete set of experimental data adopted for validation is reported in Annex A. For the application of the proposed model, the material parameters of both the flange plate and the bolts were defined according to the procedure described in Section 2.1. In particular, the quadrilinear stress-strain law adopted for the flange plate

Table 6
Proposed model vs experimental results.

	MEC-1	MEC-1.2	MEC-2	All analysed cases
$F_u/F_{u,exp}$	0.94	1.00	1.10	1.00
CoV	0.11	0.11	0.13	0.12
δ/δ_{exp}	0.93	0.88	1.15	0.94
CoV	0.20	0.21	0.12	0.19

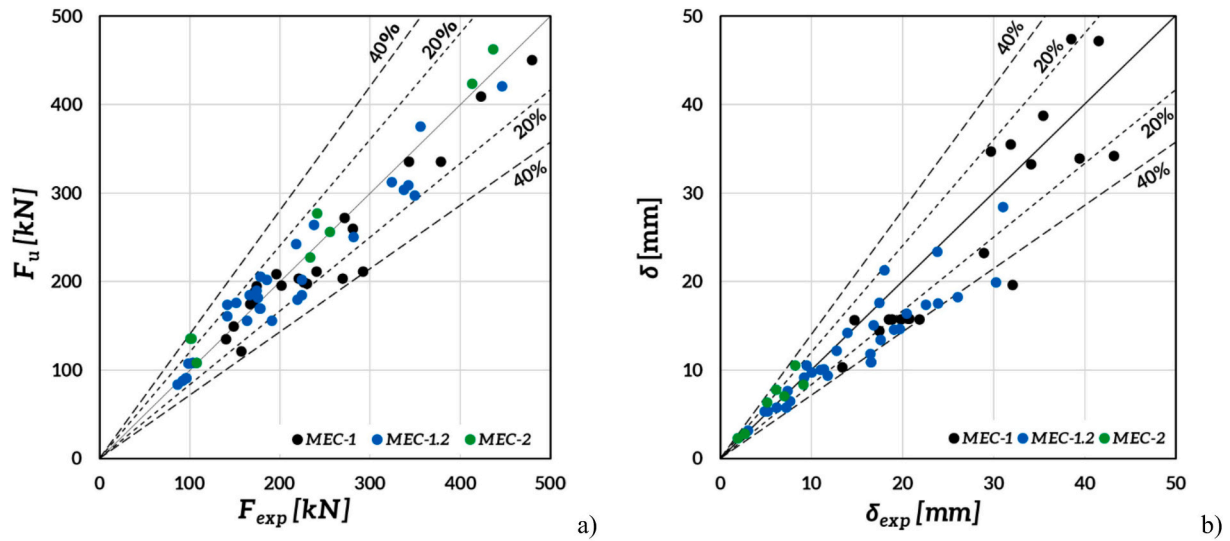


Fig. 19. Comparison between experimental results and prevision: a) ultimate resistance; b) ultimate displacement.

requires the identification of the constitutive parameters governing yielding, strain hardening, and ultimate behaviour. Whenever these parameters were not explicitly reported in the original experimental studies, they were defined consistently on the basis of the material properties proposed by Faella et al. [8] for steels of comparable strength grade. The experimental tests were then classified according to the criteria proposed in the present work. On this basis, 21 tests were identified as MEC-1, 30 as MEC-1.2, and 7 as MEC-2, whereas no test fell within the MEC-2.3 or MEC-3 ranges. It is worth noting that the majority of the cases regard MEC-1 and MEC-1.2 configurations. Therefore, the proposed formulation includes both flange dominated and bolt dominated failure modes. As already noted, when the same tests are classified according to the method proposed by Lyu et al., 40 cases are identified as MEC-1, 13 as MEC-1.2, and 5 as MEC-2. The different distribution obtained with the present classification highlights the influence of the estimated position of the resultant contact force on the identification of the governing collapse mechanism. It is noteworthy that the range of variation of the mechanical and geometrical properties for the experimental dataset is the following:

- $1.6 \leq m/t \leq 5.7$
- $0.4 \leq e/m \leq 2.1$;
- $1.2 \leq b_{eff}/m \leq 3.8$;
- $1.6 \leq m/d_b \leq 3.6$;
- $2.3 \leq f_{ub}/f_y \leq 3.7$

The overall results of the comparison between the experimental data and the analytical predictions are summarised in Table 6 and graphically illustrated in Fig. 19 and Fig. 20. With reference to ultimate resistance, the proposed formulation shows very good agreement with the experimental results, with a mean ratio $F_u/F_{u,exp}$ equal to 1.00 and a coefficient of variation equal to 0.12. A similarly satisfactory agreement is obtained for the deformation capacity, for which the mean ratio δ/δ_{exp} is equal to 0.94, with a coefficient of variation equal to 0.20. These results confirm that the proposed approach is able to provide reliable predictions not only of the collapse resistance, but also of the corresponding ultimate displacement. The accuracy of the model was also examined with respect to the flange material properties. Within the range covered by the experimental database, no clear systematic dependence of the error

on these parameters was observed. Similar levels of accuracy were obtained for S235 and S355 steels, indicating that the scatter is mainly related to the failure mode, the influence of the β threshold values or experimental uncertainties.

A closer examination of the results by failure mechanism confirms the overall good predictive performance of the proposed formulation. With reference to ultimate resistance, the agreement remains satisfactory for all the identified mechanisms. In particular, the model provides very good predictions for MEC-1 and MEC-1.2, while a slight overestimation is observed for MEC-2. A similar tendency is observed for the deformation capacity. The agreement is very good for MEC-1 and remains satisfactory for MEC-1.2, whereas a slightly lower accuracy is found for MEC-2, for which the proposed formulation tends to overestimate the ultimate displacement. This result is not unexpected, since the prediction of collapse displacement is more sensitive than strength to the modelling assumptions related to bolt elongation, flange plasticity, and mode transition. Nevertheless, the overall level of accuracy remains satisfactory also in this case, further confirming the effectiveness of the proposed simplified approach.

A detailed test-by-test comparison is reported in Annex C, including experimental ultimate resistance and displacement capacity vs the corresponding predicted values. Although a limited number of outliers can be identified within the validation dataset, the overall statistical indicators reported in Table 6 confirm that the proposed formulation provides essentially unbiased predictions of both ultimate resistance and deformation capacity. The observed scatter can be associated with identifiable causes rather than with a systematic deficiency of the proposed formulation. With regard to ultimate resistance, part of the discrepancy may be related to the redistribution of contact forces occurring during the plastic evolution of flange-dominated mechanisms (MEC-1). As discussed by Neutelaers et al. [33], this phenomenon may lead to a reduction of the effective lever arm and a corresponding increase of the actual resistance. Since the proposed formulation is intended to remain a simplified closed-form model, this advanced redistribution mechanism is not explicitly represented. With regard to deformation capacity, the prediction of ultimate displacement is inherently more sensitive than strength to fracture-related parameters, particularly the ultimate bolt strain ($\epsilon_{u,b}$) and the ultimate flange strain ($\epsilon_{u,f}$). Since these parameters are not always directly available in the

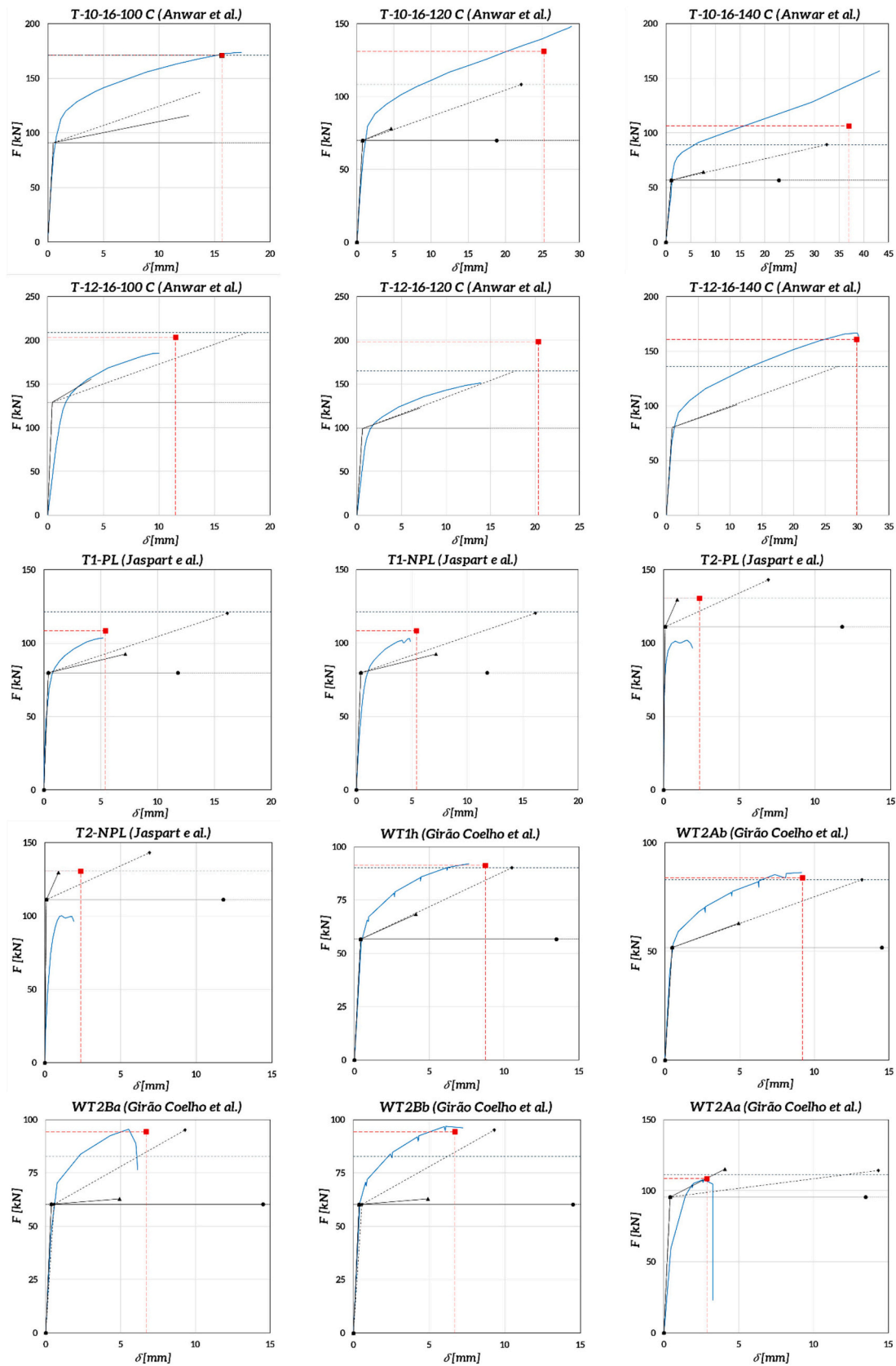


Fig. 20. Comparison between experimental curves and model predictions for the whole experimental dataset.

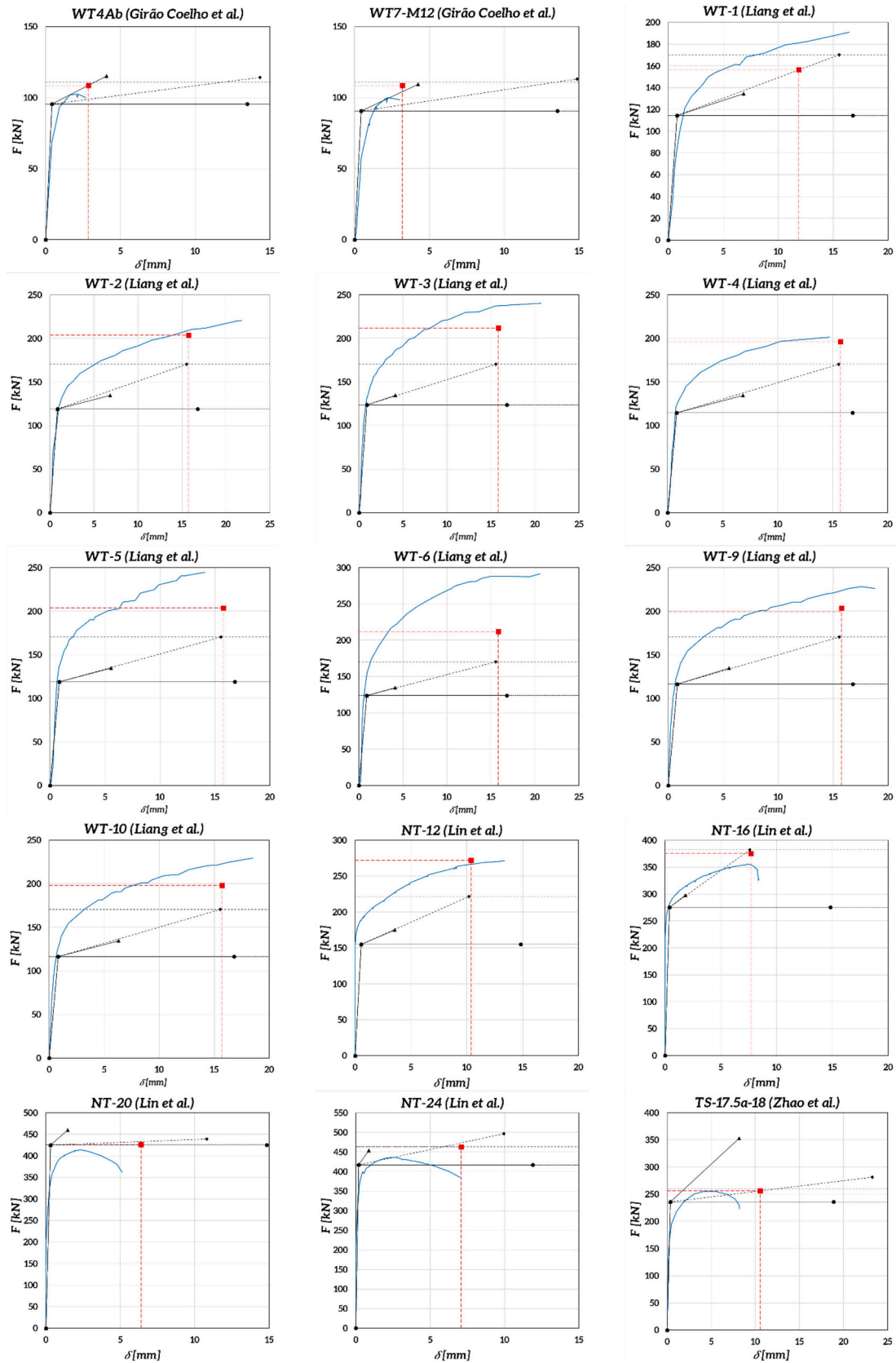


Fig. 20. (continued).

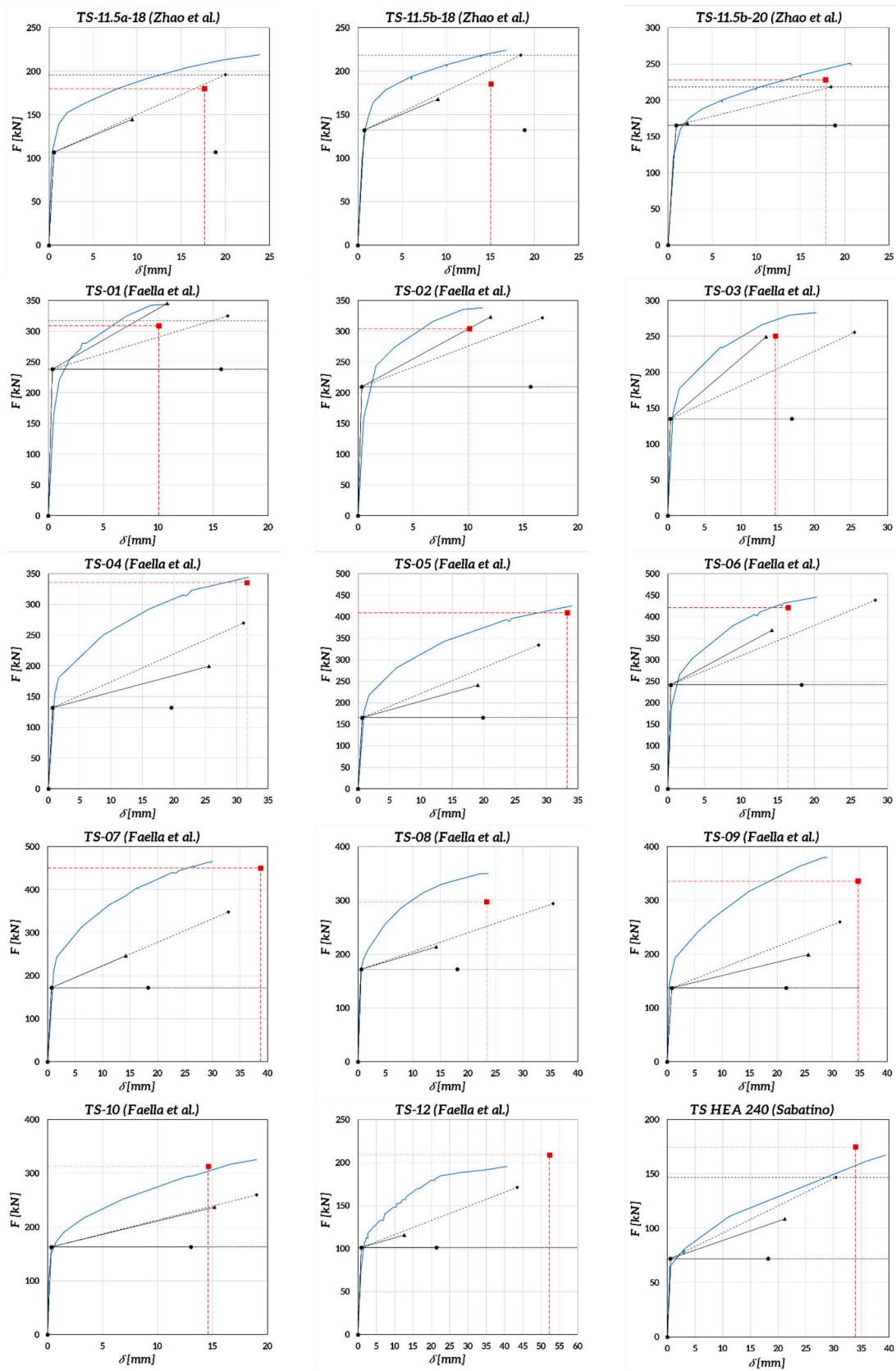


Fig. 20. (continued).

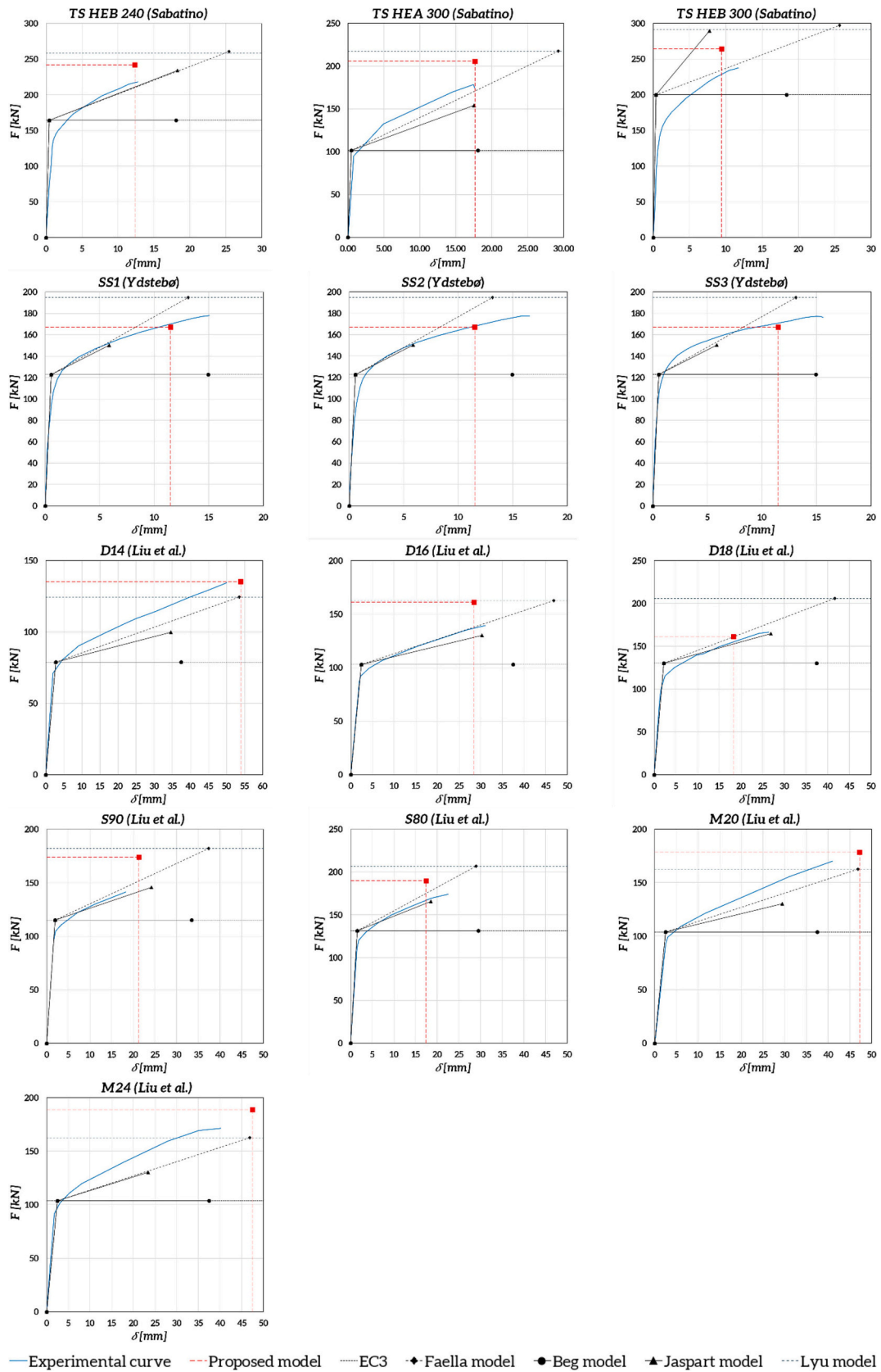


Fig. 20. (continued).

original experimental sources and often need to be estimated from literature recommendations, additional uncertainty is inevitably introduced into the prediction. Furthermore, the experimental determination of ultimate displacement is itself affected by uncertainty, especially in tests characterised by progressive fracture development. Therefore, the residual scatter observed in the validation results should be regarded as the combined effect of modelling simplifications, uncertainties associated with fracture-related parameters and the intrinsic variability of experimental measurements.

For practical design applications, it should be noted that the proposed formulation provides mean estimates of the ultimate resistance and deformation capacity. As for any predictive formulation intended for structural design applications, the residual scatter observed during the validation process should be explicitly taken into account when deriving characteristic values from the mean predictions. Following the procedure recommended in EN 1990 Annex D [38], characteristic estimates may be derived from the expected value and coefficient of variation of the prediction-to-test ratios reported in Table 6 according to the following:

$$X_k = X_{expected}(1 - k_n V_X) \quad (32)$$

where $X_{expected}$ is the expected value, V_X is the corresponding CoV, and k_n is the fractile factor. Considering the complete validation database of 58 tests and adopting $k_n = 1.64$, corresponding to the 5% characteristic fractile, the following preliminary characteristic estimates can be derived:

$$F_{u,k} \cong 0.80F_u \text{ and } \delta_k \cong 0.70\delta \quad (33)$$

These factors account for the scatter observed during the validation process and provide a rational transition from model predictions to characteristic values. In addition to this model factor, the corresponding design values may subsequently be obtained through the application also of a partial safety factor, as prescribed by EN 1993, thus accounting for additional sources of uncertainty such as, for example, other structural execution imperfections.

A more comprehensive comparison is provided in Fig. 20, where, for all the 58 experimental cases, the experimental response is compared with the predictions obtained from the proposed model and from the closed-form formulations by Faella et al. [8], Beg et al. [6], Jaspart [14], and Lyu et al. [14]. This figure allows a direct case-by-case evaluation of the capability of the different models to reproduce the overall response up to collapse. The proposed model is conceived as a complement to the EC3 Part 1–8 approach, extending the prediction of initial stiffness and plastic resistance (performed using the equations of EC3) to the evaluation of ultimate resistance and deformation capacity by means of the formulation presented in this paper. It is noteworthy that in the present work no attempt is made to recalibrate the plastic resistance proposed by EC3 or to define an experimental plastic resistance from the force–displacement curves, since the objective of the proposed formulation is the prediction of ultimate resistance and deformation capacity.

As shown in Fig. 20, the proposed formulation generally provides the closest approximation to the experimental response, both in terms of ultimate resistance and ultimate displacement. In most cases, the proposed model is able to reproduce the response with satisfactory accuracy, capturing the correct order of magnitude of the collapse displacement while also providing a realistic estimate of the maximum force. This aspect is particularly significant because it shows that the simplified transition laws introduced for the intermediate mechanisms do not only improve the statistical indicators, but also lead to a more consistent description of the experimental behaviour at the level of individual tests. By contrast, the other closed-form models exhibit clear systematic deviations. In several cases, the formulations by Beg and Jaspart et al. appear conservative in terms of strength, whereas the model by Faella et al., although often able to provide reasonable resistance estimates, tends to show larger discrepancies in the prediction of

ultimate displacement, especially when the response is influenced by bolt rupture and mechanism transition. The comparison also indicates that the proposed formulation generally performs better than the model by Lyu et al., mainly because the revised classification framework and the updated evaluation of the contact force position allow a more consistent interpretation of the governing mechanism. Overall, the comparisons reported in Fig. 20 confirm that the proposed model offers a balance between simplicity and predictive capability leading to prediction more accurate than the other available closed-form formulations. The improvement is particularly evident in those cases where the experimental response is governed by intermediate mechanisms, for which the transition between flange-dominated and bolt-dominated behaviour plays a decisive role.

5. Conclusions

This study proposed a simplified closed-form model for predicting the ultimate resistance and deformation capacity of bolted T-stubs, with the aim of overcoming the main limitations of existing closed-form formulations. The proposed approach combines mechanical consistency with practical applicability. It was verified through a parametric study and validated against experimental results available in the literature. The main conclusions can be summarised as follows:

- The proposed formulation adopts a five-mechanism classification, including the classical modes MEC-1, MEC-2, and MEC-3, together with the two transition mechanisms MEC-1.2 and MEC-2.3, thereby providing a more realistic description of the gradual transition between flange-dominated and bolt-dominated behaviour.
- A key improvement of the model lies in the mechanically consistent evaluation of the position of the resultant contact force. This leads to a revised definition of the parameter λ , updated β -boundaries, and a more accurate identification of the governing collapse mechanism.
- The proposed five-mechanism classification also showed good consistency with the reference mechanical model, correctly identifying the governing failure mechanism in 88.4% of the 138 synthetic cases.
- For the prediction of ultimate resistance, the model combines the revised failure-mode classification with a simple yet mechanically consistent estimate of the flange ultimate bending resistance, achieving a satisfactory balance between accuracy and simplicity.
- For the prediction of deformation capacity, the formulation combines different mechanical approaches depending on the governing mechanism: a Faella-type formulation for MEC-1, a Beg-type kinematic approach for MEC-2 and MEC-3, and specific transition laws for MEC-1.2 and MEC-2.3.
- The parametric verification carried out on 138 cases showed good overall agreement with the reference mechanical model, with mean values of $F_u/F_{u,par} = 1.08$ and $\delta/\delta_{par} = 0.98$, and corresponding coefficients of variation equal to 0.12 and 0.14.
- Validation against 58 experimental results further confirmed the accuracy of the proposed formulation, yielding mean values of $F_u/F_{u,exp} = 1$ and $\delta/\delta_{exp} = 0.94$, with coefficients of variation equal to 0.12 and 0.20, respectively.
- Overall, the proposed formulation provides a favourable balance between simplicity and predictive capability compared with existing closed-form models, particularly in the estimation of deformation capacity. Nevertheless, its field of application is limited to conventional coupled bolted T-stubs falling within the range of geometries considered in the validation dataset. Isolated T-stubs tied to rigid bases, where large-displacement mechanisms, bolt shear interaction, and pronounced geometric nonlinearities may significantly influence the response, fall outside the scope of the present model. Moreover, considering the scatter observed in Annex C, a specific safety-factor calibration would be required before adopting the proposed formulation in a code-based design framework.

Annex A

Literature test data.

Author	Test	Plate Data: Geometry				Bolt Data: Geometry				
		m [mm]	n [mm]	b _{eff} [mm]	t [mm]	d _b [mm]	d _w [mm]	t _{bh} [mm]	t _n [mm]	t _w [mm]
Faella et al. [8]	TS-01	39.30	40.80	126.50	14.40	20	37	13	16	4
	TS-02	39.30	40.80	119.00	14.60	20	37	13	16	4
	TS-03	42.30	22.20	124.00	13.00	20	37	13	16	4
	TS-04	49.00	58.10	118.80	12.30	24	44	15	19	4
	TS-05	49.75	74.90	115.00	13.80	24	44	15	19	4
	TS-06	45.70	45.20	120.00	16.30	24	44	15	19	4
	TS-07	52.50	71.70	123.00	13.90	27	56	17	22	5
	TS-08	45.20	19.30	113.00	13.30	24	44	15	19	4
	TS-09	54.10	53.00	125.00	12.50	27	56	17	22	5
	TS-10	32.60	58.50	125.00	10.85	20	37	13	16	4
	TS-12	53.50	49.90	90.25	12.20	20	37	13	16	4
	T-10-16-100C	37.08	30.00	90.00	10.00	16	30	10	13	3
Anwar [20]	T-10-16-120C	47.08	30.00	90.00	10.00	16	30	10	13	3
	T-10-16-140C	57.08	30.00	90.00	10.00	16	30	10	13	3
	T-12-16-100C	37.08	30.00	90.00	12.00	16	30	10	13	3
	T-12-16-120C	47.08	30.00	90.00	12.00	16	30	10	13	3
	T-12-16-140C	57.08	30.00	90.00	12.00	16	30	10	13	3
	WT1	42.00	50.00	100.00	10.00	16	30	10	13	3
Liang et al. [21]	WT2	42.00	50.00	100.00	10.00	20	37	13	16	3
	WT3	42.00	50.00	100.00	10.00	24	44	15	19	3
	WT4	42.00	50.00	100.00	10.00	16	30	10	13	3
	WT5	42.00	50.00	100.00	10.00	20	37	13	16	3
	WT6	42.00	50.00	100.00	10.00	24	44	15	19	3
	WT9	42.00	75.00	100.00	10.00	20	37	13	13	3
Lin et al. [22]	WT10	42.00	87.50	100.00	10.00	20	37	13	16	3
	NT12	37.08	45.00	80.00	12.00	20	37	13	16	3
	NT16	37.08	45.00	80.00	16.00	20	37	13	16	3
	NT20	37.08	45.00	80.00	20.00	20	37	13	16	3
	NT24	37.08	45.00	80.00	24.00	20	37	13	16	3
	SS-1	37.35	45.00	110.00	10.00	16	30	10	13	3
Ydstebo [23]	SS-2	37.35	45.00	110.00	10.00	16	30	10	13	3
	SS-3	37.35	45.00	110.00	10.00	16	30	10	13	3
	TS-17.5a-18	47.20	50.00	120.00	17.50	18	34	11	16	4
Zhao et al [14]	TS-11.5a-18	47.20	50.00	120.00	11.50	18	34	11	16	4
	TS-11.5b-18	47.20	50.00	120.00	11.50	18	34	11	16	4
	TS-11.5b-20	47.20	50.00	120.00	11.50	20	37	13	16	4
	TS HEA 240	45.45	54.00	68.00	12.00	18	34	11	16	4
Sabatino [34]	TS HEB 240	45.20	53.00	68.00	17.00	18	34	11	16	4
	TS HEA 300	45.15	79.00	68.00	14.00	18	34	11	16	4
	TS HEB 300	45.90	77.00	68.00	19.00	18	34	11	16	4
Jaspart et al [24]	T1-PL	29.45	30.00	40.00	10.70	12	24	8	10	3
	T1-NPL	29.45	30.00	40.00	10.70	12	24	8	10	3
	T2-PL	25.85	30.00	40.00	16.00	12	24	8	10	3
	T2-NPL	25.85	30.00	40.00	16.00	12	24	8	10	3
	WT1h	33.73	30.00	45.05	10.32	12	24	8	10	2.5
	WT2Ab	36.29	29.90	45.00	10.27	12	24	8	10	2.5
Girão Coelho [25]	WT2Ba	31.69	29.90	44.95	10.29	12	24	8	10	2.5
	WT2Bb	31.69	29.90	44.95	10.29	12	24	8	10	2.5
	WT4Ab	33.69	30.10	74.85	10.39	12	24	8	10	2.5
	WT7_M12	33.87	29.90	75.60	10.09	12	24	8	10	2.5
	WT4Aa	33.69	30.10	74.85	10.39	12	24	8	10	2.5
	D14	93.60	60.00	80.00	14.00	18	34	11	16	4.0
Liu [35]	D16	93.60	60.00	80.00	16.00	18	34	11	16	4.0
	D18	93.60	60.00	80.00	18.00	18	34	11	16	4.0
	S90	83.60	70.00	80.00	16.00	18	34	11	16	4.0
	S80	73.60	80.00	80.00	16.00	18	34	11	16	4.0
	M20	93.60	60.00	80.00	16.00	22	37	13	19	4.0
	M24	93.60	60.00	80.00	16.00	27	56	17	22	5.0

Author	Test	Plate Data: Material						Bolt Data: Material		Test results	
		E _b [Mpa]	E _u [Mpa]	ε _b	ε _u	f _y [Mpa]	f _u [Mpa]	ε _{b,u}	f _{ub} [Mpa]	F _u [kN]	δ [mm]
Faella et al. [8]	TS-01	3280	371	0.014	0.49	291	517	0.18	904	342.00	10.95
	TS-02	3170	378	0.011	0.48	265	501	0.18	904	337.00	11.35
	TS-03	3090	436	0.012	0.59	273	504	0.18	904	281.00	19.65
	TS-04	2877	465	0.013	0.77	300	544	0.18	904	343.00	31.85
	TS-05	3340	483	0.014	0.78	318	547	0.18	904	423.00	34.10
	TS-06	3360	488	0.012	0.64	280	528	0.18	904	446.00	20.40
	TS-07	3420	516	0.013	0.78	308	544	0.18	904	479.00	35.40
	TS-08	3280	477	0.011	0.69	269	483	0.18	904	349.00	23.80
	TS-09	3230	467	0.013	0.68	301	552	0.18	904	378.00	29.70
	TS-10	2160	406	0.01	0.93	293	525	0.14	1034	324.00	19.00
	TS-12	2310	383	0.01	0.95	347	461	0.18	904	196.00	40.50

(continued on next page)

Annex A (continued)

Author	Test	Plate Data: Material						Bolt Data: Material		Test results	
		E_h [Mpa]	E_u [Mpa]	ε_h	ε_u	f_y [Mpa]	f_u [Mpa]	$\varepsilon_{b,u}$	f_{ub} [Mpa]	F_u [kN]	δ [mm]
Anwar [20]	T-10-16-100C	4907	470	0.016	0.56	310	408	0.14	1020	173.70	17.41
	T-10-16-120C	4907	470	0.016	0.56	310	408	0.14	1020	148.21	28.93
	T-10-16-140C	4907	470	0.016	0.56	310	408	0.14	1020	156.64	43.19
	T-12-16-100C	4907	470	0.016	0.56	305	445	0.14	1020	185.17	10.00
	T-12-16-120C	4907	470	0.016	0.56	305	445	0.14	1020	150.95	13.91
	T-12-16-140C	4907	470	0.016	0.56	305	445	0.14	1020	163.30	30.26
Liang et al. [21]	WT1	4907	470	0.022	0.56	418	565	0.14	947	190.80	16.45
	WT2	4907	470	0.022	0.56	418	565	0.14	947	220.25	21.80
	WT3	4907	470	0.022	0.56	418	565	0.14	947	240.53	20.70
	WT4	4907	470	0.022	0.56	418	565	0.14	1100	201.32	14.70
	WT5	4907	470	0.022	0.56	418	565	0.14	1100	269.27	19.80
	WT6	4907	470	0.022	0.56	418	565	0.14	1100	291.64	20.60
	WT9	4907	470	0.022	0.56	418	565	0.14	947	226.20	18.80
	WT10	4907	470	0.022	0.56	418	565	0.14	947	229.53	18.50
	NT12	4907	470	0.020	0.56	406	563	0.11	1135	271.31	13.38
	NT16	4907	470	0.020	0.56	406	539	0.11	1135	355.48	7.33
Lin et al. [22]	NT20	4907	470	0.020	0.56	401	533	0.11	1135	413.10	5.10
	NT24	4907	470	0.020	0.56	399	529	0.11	1135	436.19	7.01
Ydstebo [23]	SS-1	4907	470	0.019	0.56	370	560	0.18	904	177.87	15.03
	SS-2	4907	470	0.019	0.56	370	560	0.18	904	177.36	16.50
	SS-3	4907	470	0.019	0.56	370	560	0.18	904	178.38	15.85
Zhao et al. [14]	TS-17.5a-18	4907	470	0.014	0.56	261	453	0.185	820	255.18	8.21
	TS-11.5a-18	4907	470	0.014	0.56	274	430	0.185	820	218.92	23.87
	TS-11.5b-18	4907	470	0.018	0.56	339	499	0.185	820	223.96	16.76
	TS-11.5b-20	4907	470	0.018	0.56	339	499	0.193	836	280.47	32.06
Sabatino [34]	TS HEA 240	2835	338	0.02	0.93	287	504	0.14	904	167.12	39.40
	TS HEB 240	2163	335	0.015	0.77	325	548	0.14	904	218.00	12.75
	TS HEA 300	3040	373	0.016	1.00	302	522	0.14	904	178.43	17.44
	TS HEB300	5050	368	0.017	0.83	328	541	0.14	904	237.50	11.75
Jaspart et al [24]	T1-PL	4907	470	0.023	0.56	431	595	0.07	974	103.46	5.20
	T1-NPL	4907	470	0.023	0.56	431	595	0.07	974	103.00	4.80
	T2-PL	4907	470	0.015	0.56	282	483	0.06	945	101.69	1.91
	T2-NPL	4907	470	0.015	0.56	282	483	0.06	945	100.00	1.91
Girão Coelho [25]	WT1h	4907	470	0.018	0.56	340	480	0.07	880	92.05	7.64
	WT2Ab	4907	470	0.018	0.56	340	480	0.07	880	86.25	9.15
	WT2Ba	4907	470	0.018	0.56	340	480	0.07	880	95.51	6.14
	WT2Bb	4907	470	0.018	0.56	340	480	0.07	880	96.14	7.23
	WT4Ab	4907	470	0.018	0.56	340	480	0.07	920	107.70	2.67
	WT7_M12	4907	470	0.018	0.56	340	480	0.07	920	98.40	3.03
	WT4Aa	4907	470	0.018	0.56	340	480	0.07	920	107.70	2.67
	D14	4907	470	0.023	0.56	431	595	0.14	1000	140.00	55.00
Liu [35]	D16	4907	470	0.023	0.56	431	595	0.14	1000	141.00	31.00
	D18	4907	470	0.023	0.56	431	595	0.14	1000	166.00	26.00
	S90	4907	470	0.023	0.56	431	595	0.14	1000	141.00	18.00
	S80	4907	470	0.023	0.56	431	595	0.14	1000	174.00	22.50
	M20	4907	470	0.023	0.56	431	595	0.14	1000	170.00	41.50
	M24	4907	470	0.023	0.56	431	595	0.14	1000	172.00	38.50

Annex B

Synthetic data based on mechanical modelling.

#	e/m	m/t	m/d_b	b_{eff}/m	f_{ub}/f_y	F_u [kN]	δ_u [mm]	#	e/m	m/t	m/d_b	b_{eff}/m	f_{ub}/f_y	F_u [kN]	δ [mm]
1	0.30	4	3.5	1.50	2.25	148.18	10.48	70	0.78	6	2.0	3.00	4.26	391.87	5.23
2	0.30	6	3.5	1.50	2.25	130.74	21.55	71	0.78	8	2.0	3.00	3.83	250.80	4.52
3	0.30	4	5.0	1.50	2.25	400.27	14.83	72	0.78	4	3.5	3.00	4.26	250.80	4.46
4	0.30	8	5.0	1.50	2.25	188.83	23.82	73	0.78	4	5.0	3.00	4.26	391.87	6.04
5	0.30	4	3.5	3.00	2.25	123.11	15.21	74	0.78	6	5.0	3.00	4.26	564.30	6.77
6	0.30	8	3.5	3.00	2.25	142.73	23.92	75	0.78	4	2.0	4.50	4.26	714.19	7.45
7	0.30	4	5.0	3.00	2.25	186.48	8.69	76	0.78	6	2.0	4.50	4.26	58.41	7.73
8	0.30	6	5.0	3.00	2.25	155.19	24.53	77	0.78	8	2.0	4.50	3.83	42.36	14.26
9	0.30	10	5.0	3.00	2.25	162.07	27.21	78	0.78	4	5.0	4.50	4.26	166.07	13.01
10	0.30	4	3.5	4.38	2.25	191.43	43.60	79	0.78	8	5.0	4.50	4.26	63.20	17.15
11	0.30	6	3.5	4.38	2.25	510.78	13.42	80	0.78	10	5.0	4.50	4.26	43.84	19.08
12	0.30	8	3.5	4.38	2.25	271.66	17.61	81	0.78	2	3.5	1.50	4.26	117.80	17.04
13	0.30	10	3.5	4.38	2.25	113.86	24.75	82	0.78	2	5.0	1.50	4.26	421.15	26.90
14	0.30	4	5.0	4.38	2.25	69.10	26.97	83	0.78	2	5.0	3.00	4.26	122.24	16.01
15	0.30	6	5.0	4.38	2.25	179.84	16.49	84	0.78	2	2.0	4.50	4.26	188.20	11.05
16	0.30	8	5.0	4.38	2.25	201.07	31.71	85	0.78	2	3.5	4.50	4.26	540.12	15.29
17	0.30	2	3.5	1.50	2.25	120.14	10.55	86	0.78	2	5.0	4.50	4.26	164.27	16.74
18	0.30	2	5.0	1.50	2.25	370.10	15.06	87	0.78	6	3.5	1.50	2.25	181.98	26.40
19	0.30	2	3.5	3.00	2.25	189.27	7.68	88	0.78	6	3.5	3.00	2.25	116.71	7.78
20	0.30	2	5.0	3.00	2.25	167.94	17.07	89	0.78	6	3.5	4.50	2.25	84.69	14.29
21	0.30	2	3.5	4.38	2.25	188.82	21.61	90	0.78	2	3.5	3.00	2.25	124.17	17.16
22	0.30	2	5.0	4.38	2.25	523.87	10.84	91	0.78	6	3.5	4.50	4.26	67.88	18.07
23	0.30	4	3.5	1.50	4.26	281.88	14.60	92	0.78	2	3.5	3.00	4.26	205.16	9.04
24	0.30	4	5.0	1.50	4.26	132.50	19.55	93	1.25	4	3.5	1.50	2.25	169.49	18.83
25	0.30	6	5.0	1.50	4.26	183.38	14.98	94	1.25	6	3.5	1.50	2.25	543.88	12.83
26	0.30	4	3.5	3.00	4.26	204.36	24.27	95	1.25	4	5.0	1.50	2.25	166.27	15.16
27	0.30	6	3.5	3.00	4.26	242.95	8.15	96	1.25	6	5.0	1.50	2.25	179.91	19.53
28	0.30	4	5.0	3.00	4.26	225.41	24.95	97	1.25	8	5.0	1.50	2.25	249.59	7.70
29	0.30	8	5.0	3.00	4.26	694.67	11.30	98	1.25	10	5.0	1.50	2.25	212.82	24.47
30	0.30	4	3.5	4.38	4.26	360.35	12.20	99	1.25	4	2.0	3.00	2.25	215.74	25.83
31	0.30	6	3.5	4.38	4.26	159.16	22.85	100	1.25	4	3.5	3.00	2.25	685.08	12.14
32	0.30	4	5.0	4.38	4.26	250.80	7.72	101	1.25	6	3.5	3.00	2.25	382.28	14.83
33	0.30	6	5.0	4.38	4.26	240.35	16.38	102	1.25	8	3.5	3.00	2.25	249.61	16.53
34	0.30	8	5.0	4.38	4.26	258.80	25.25	103	1.25	4	5.0	3.00	2.25	264.70	26.51
35	0.30	10	5.0	4.38	4.26	155.41	7.98	104	1.25	8	5.0	3.00	2.25	127.00	14.32
36	0.30	2	5.0	1.50	4.26	427.09	10.88	105	1.25	4	2.0	4.50	2.25	460.53	13.37
37	0.30	2	3.5	3.00	4.26	219.57	6.76	106	1.25	4	3.5	4.50	2.25	185.11	17.18
38	0.30	2	5.0	3.00	4.26	236.00	26.12	107	1.25	10	3.5	4.50	2.25	92.17	17.75
39	0.30	2	3.5	4.38	4.26	613.73	10.52	108	1.25	6	5.0	4.50	2.25	342.57	16.81
40	0.30	2	5.0	4.38	4.26	319.17	12.32	109	1.25	10	5.0	4.50	2.25	210.32	15.07
41	0.78	4	3.5	1.50	2.25	84.58	18.62	110	1.25	2	3.5	1.50	2.25	204.51	15.77
42	0.78	4	5.0	1.50	2.25	244.82	14.93	111	1.25	2	5.0	1.50	2.25	263.35	25.12
43	0.78	6	5.0	1.50	2.25	209.57	15.27	112	1.25	2	2.0	3.00	2.25	262.66	27.58
44	0.78	8	5.0	1.50	2.25	225.05	16.90	113	1.25	2	3.5	3.00	2.25	810.56	11.85
45	0.78	10	5.0	1.50	2.25	259.01	30.90	114	1.25	2	5.0	3.00	2.25	231.86	15.56
46	0.78	4	2.0	3.00	2.25	302.56	9.96	115	1.25	2	2.0	4.50	2.25	95.01	20.75
47	0.78	4	3.5	3.00	2.25	268.75	26.02	116	1.25	2	3.5	4.50	2.25	313.50	13.49
48	0.78	8	3.5	3.00	2.25	714.19	7.19	117	1.25	2	5.0	4.50	2.25	280.65	16.52
49	0.78	4	5.0	3.00	2.25	433.44	10.44	118	1.25	4	2.0	1.50	4.26	294.81	17.96
50	0.78	6	5.0	3.00	2.25	98.17	19.83	119	1.25	6	2.0	1.50	4.26	331.18	34.22
51	0.78	8	5.0	3.00	2.25	250.80	5.00	120	1.25	4	3.5	1.50	4.26	271.48	7.83
52	0.78	4	2.0	4.50	2.25	308.76	16.16	121	1.25	4	5.0	1.50	4.26	416.94	11.27
53	0.78	4	3.5	4.50	2.25	297.78	16.78	122	1.25	4	2.0	3.00	4.26	892.73	12.89
54	0.78	10	3.5	4.50	2.25	250.80	6.54	123	1.25	6	2.0	3.00	4.26	892.73	11.68
55	0.78	4	5.0	4.50	2.25	391.87	7.69	124	1.25	4	3.5	3.00	4.26	705.37	9.40
56	0.78	6	5.0	4.50	2.25	564.30	8.81	125	1.25	6	3.5	3.00	4.26	489.84	7.85
57	0.78	10	5.0	4.50	2.25	714.19	8.10	126	1.25	4	5.0	3.00	4.26	705.37	8.24
58	0.78	2	3.5	1.50	2.25	714.19	7.90	127	1.25	6	5.0	3.00	4.26	892.73	8.32
59	0.78	2	5.0	1.50	2.25	564.30	6.95	128	1.25	8	5.0	3.00	4.26	313.50	5.36
60	0.78	2	2.0	3.00	2.25	357.08	7.44	129	1.25	6	2.0	4.50	4.26	489.84	6.12
61	0.78	2	5.0	3.00	2.25	236.16	8.80	130	1.25	8	5.0	4.50	4.26	705.37	6.82
62	0.78	2	2.0	4.50	2.25	391.87	5.36	131	1.25	10	5.0	4.50	4.26	771.75	8.78
63	0.78	2	3.5	4.50	2.25	564.30	5.89	132	1.25	2	3.5	1.50	4.26	763.46	11.13
64	0.78	2	5.0	4.50	2.25	714.19	6.39	133	1.25	2	5.0	1.50	4.26	489.84	5.54
65	0.78	4	2.0	1.50	4.26	250.80	5.15	134	1.25	2	3.5	3.00	4.26	313.50	4.72
66	0.78	6	2.0	1.50	4.26	391.87	6.07	135	1.25	2	5.0	3.00	4.26	313.50	4.61
67	0.78	8	2.0	1.50	3.83	564.30	6.77	136	1.25	2	2.0	4.50	4.26	489.84	6.09
68	0.78	4	3.5	1.50	4.26	714.19	9.81	137	1.25	2	3.5	4.50	4.26	705.37	6.82
69	0.78	4	5.0	1.50	4.26	714.19	7.01	138	1.25	2	5.0	4.50	4.26	892.73	7.48

Annex C

Experimental validation database.

Author	Test	Test results		Predicted values		$F_u / F_{u,exp}$	δ / δ_{exp}
		$F_{u,exp}$ [kN]	δ_{exp} [mm]	F_u [kN]	δ [mm]		
Faella et al. [8]	TS-01	342.00	10.95	309.07	10.07	0.90	0.92
	TS-02	337.00	11.35	304.37	10.15	0.90	0.89
	TS-03	281.00	19.65	250.38	14.68	0.89	0.75
	TS-04	343.00	31.85	335.55	35.54	0.98	1.12
	TS-05	423.00	34.10	409.77	33.30	0.97	0.98
	TS-06	446.00	20.40	421.06	16.42	0.94	0.80
	TS-07	479.00	35.40	450.23	38.75	0.94	1.09
	TS-08	349.00	23.80	297.48	23.41	0.85	0.98
	TS-09	378.00	29.70	335.76	34.72	0.89	1.17
	TS-10	324.00	19.00	312.53	14.61	0.96	0.77
	TS-12	196.00	40.50	208.67	52.30	1.06	1.29
Anwar [20]	T-10-16-100C	173.70	17.41	195.30	14.48	1.12	0.83
	T-10-16-120C	148.21	28.93	149.67	23.30	1.01	0.81
	T-10-16-140C	156.64	43.19	121.32	34.20	0.77	0.79
	T-12-16-100C	185.17	10.00	199.46	10.22	1.08	1.02
	T-12-16-120C	150.95	13.91	172.98	15.70	1.15	1.13
	T-12-16-140C	163.30	30.26	172.87	28.79	1.06	0.95
	WT1	190.80	16.45	156.43	11.88	0.82	0.72
	WT2	220.25	21.80	203.67	15.77	0.92	0.72
	WT3	240.53	20.70	212.00	15.83	0.88	0.76
	WT4	201.32	14.70	196.11	15.71	0.97	1.07
	WT5	269.27	19.80	203.67	15.77	0.76	0.80
	WT6	291.64	20.60	212.00	15.83	0.73	0.77
Liang et al. [21]	WT9	226.20	18.80	199.26	15.74	0.88	0.84
	WT10	229.53	18.50	198.06	15.73	0.86	0.85
	NT12	271.31	13.38	272.20	10.37	1.00	0.78
	NT16	355.48	7.33	375.25	7.70	1.06	1.05
	NT20	413.10	5.10	424.11	6.39	1.03	1.25
	NT24	436.19	7.01	463.11	7.08	1.06	1.01
Lin et al. [22]	SS-1	177.87	15.03	169.97	10.92	0.96	0.66
	SS-2	177.36	16.50	169.97	10.92	0.96	0.66
	SS-3	178.38	15.85	169.97	10.92	0.96	0.66
Ydstebo [23]	TS-17.5a-18	255.18	8.21	256.15	10.54	1.00	1.28
	TS-11.5a-18	218.92	23.87	179.86	17.60	0.82	0.74
	TS-11.5b-18	223.96	16.76	185.12	15.08	0.83	0.90
	TS-11.5b-20	280.47	32.06	260.00	19.62	0.93	0.61
	TS HEA 240	167.12	39.40	174.79	33.95	1.05	0.86
	TS HEB 240	218.00	12.75	242.60	12.24	1.11	0.96
Zhao et al. [14]	TS HEA 300	178.43	17.44	205.82	17.63	1.15	1.01
	TS HEB300	237.50	11.75	264.38	9.40	1.11	0.80
	T1-PL	103.46	5.20	108.45	5.40	1.05	1.04
	T1-NPL	103.00	4.80	108.45	5.40	1.05	1.13
	T2-PL	101.69	1.91	135.72	2.36	1.33	1.24
	T2-NPL	100.00	1.91	135.72	2.36	1.36	1.24
Sabatino [34]	WT1h	92.05	7.64	88.21	6.53	0.96	0.85
	WT2Ab	86.25	9.15	83.86	9.21	0.97	1.01
	WT2Ba	95.51	6.14	91.26	5.82	0.96	0.95
	WT2Bb	96.14	7.23	91.26	5.82	0.95	0.80
	WT4Ab	107.70	2.67	108.51	2.87	1.01	1.07
	WT7_M12	98.40	3.03	107.69	3.19	1.09	1.05
Girão Coelho [25]	WT4Aa	107.70	2.67	108.51	2.87	1.01	1.07
	D14	140.00	55.00	135.35	53.92	0.97	0.98
	D16	141.00	31.00	161.14	28.45	1.14	0.92
	D18	166.00	26.00	184.94	18.28	1.11	0.70
	S90	141.00	18.00	173.93	21.29	1.23	1.18
	S80	174.00	22.50	189.83	17.42	1.09	0.77
Liu [35]	M20	170.00	41.50	178.29	47.22	1.05	1.14
	M24	172.00	38.50	188.64	47.46	1.10	1.23
					Mean value	1.00	0.94
					CoV	0.12	0.19

CRediT authorship contribution statement

A.B. Francavilla: Writing – review & editing, Writing – original draft, Validation, Software, Methodology, Investigation, Formal analysis, Data curation, Conceptualization. **M. Latour:** Writing – review & editing, Validation, Supervision, Methodology, Investigation, Formal analysis, Data curation, Conceptualization. **J.F. Demonceau:** Writing – review & editing, Validation, Conceptualization. **A. Neutels:** Writing

– review & editing, Conceptualization. **G. Rizzano:** Writing – review & editing, Supervision, Methodology, Conceptualization.

Declaration of competing interest

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Data availability

Data will be made available on request.

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