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**MICRO-VOID EVOLUTION  
AND  
DUCTILE FRACTURE BEHAVIOUR  
IN  
HIGH STRENGTH S690 STEEL MEMBERS AND  
CONNECTIONS**

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Micro-Void Evolution and Ductile Fracture Behaviour in High  
Strength S690 Steel Members and Connections

LI MENGFEI

A thesis submitted in partial fulfilment of the requirements for  
the degree of Doctor of Philosophy

August 2025

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# ABSTRACT

In recent years, the construction industry worldwide has witnessed a significant increase in use of high strength S690 steel. This increase can be attributed to exceptional strength-to-self-weight ratios offered by the high strength steel, enabling implementation of lightweight and efficient structural solutions in construction. The high strength steel is expected to have many potential applications in construction, such as long span bridges and high-rise buildings, in many parts of the world. It is important to understand various mechanical properties of the high strength steel in order to enable their effective use in construction, and many investigations into the mechanical properties of the steel and their welded sections are available in the literature. While both strengths and ductility are of primary importance, fracture is widely considered to be essential to materials researchers as well as mechanical and structural engineers to prevent failure of structural members and connections at large deformations.

The primary objective of this research is to develop a comprehensive, experimentally validated ductile fracture model for the high strength S690 steel members and bolted connections under combined tension and shear actions. The scope of work includes:

1. **To develop a constitutive model** for the high strength S690 steel by modifying the Bridgman method for precise determination of true stress-strain curves after allowing for non-uniform stress distributions.
2. **To study evolution of micro-voids of the S690 steel** using in-situ X-ray computed tomography (CT), and to examine void nucleation and growth up to fracture initiation.
3. **To formulate a fracture initiation criterion for the S690 steel** that incorporates stress triaxiality and Lode angle effects to predict their ductile fracture under combined tension and shear actions.
4. **To validate the proposed fracture model** through structural testing of bolted connections of the S690 steel with various configurations to ensure applicability in structural design.

This project aims to bridge the gap between material-level fracture mechanisms and fracture behaviour of structural members and bolted connections, ultimately providing engineers with robust tools to assess structural performance and safety of high strength steel structures.

Key research findings are summarized as follows:

**1) Determination of true stress-strain curves through Modified Bridgman method**

By standard tensile tests and high-resolution digital imaging conducted on cylindrical coupons of S355 to S960 steel, the instantaneous dimensions of their necking regions, in particular, the radii of the minimum cross-sectional areas ( $a$ ) and the longitudinal curvature ( $1/R$ ), are precisely tracked throughout the entire loading processes. *An improved cubic function for the spatial radius ratio  $a/R$  is then developed, and it is embedded into the Bridgman method to determine true stresses accurately to allow for non-uniform stress distributions in the necking regions.* This Modified Bridgman method is demonstrated to be able to give accurate true stress-strain curves for determination of load-extension curves in standard tensile tests up to fracture, which is the basis of subsequent research.

**2) Evolution of micro-voids under tension**

Micro-void evolution of S355 to S960 steels under tension is explored by using in-situ X-ray computed tomography (CT) to examine their deformation characteristics from necking to fracture initiation at microscopic levels. High-resolution 3D scanning and imaging technique is employed to capture newly born micro-voids as well as those voids with increased sizes in these three different steels up to fracture. The increase in the numbers and the sizes of the voids at the critical cross-sections are readily quantified up to fracture initiation. *Despite there are significant increases in both the numbers and the sizes of these voids when the applied tension forces are increased, the total void volumes are found to be smaller than 0.2% of the regions of interest of the steel coupons, i.e. too small to affect post-necking softening of the coupons significantly.* By combining experimental

and computational results, a new understanding on micro-void evolution is readily attained to advance prediction capabilities on microstructure-informed fracture in structural steels.

### 3) **Development of a ductile fracture model**

A new ductile fracture model incorporating the effects of both tension and shear actions in the high strength S690 steel is proposed. A total of 34 specimens, i.e. five different types of notched specimens with different stress triaxialities and Lode parameters, are tested under tension. Advanced numerical models have also been established to predict the corresponding critical states of *stresses and strains* of the specimens up to the points of fracture initiation. *After systematic analyses on both the test and the numerical data, the influences of the stress triaxialities and the Lode parameters are readily quantified. Hence, the proposed model is demonstrated to be able to describe the ductile fracture behaviour of the S690 steel under a wide range of combined tension and shear actions commonly encountered in practical situations with a high level of accuracy.* After comparing with five classical and yet conservative ductile fracture models, the proposed model shows superior accuracy in predicting fracture behaviour of high strength S690 steel.

### 4) **Accurate prediction on fracture behaviour of bolted connections**

In order to apply the proposed ductile fracture model to bolted connections of specific steel plates, it is necessary to calibrate various parameters of the model, including material-specific parameters, through a series of tensile tests on six different types of coupons of the steel plates. After rigorous data analysis and formulation, the proposed model is then applied to predict the load-extension curves of a total of 6 bolted connections under various combinations of tension and shear actions. *It is demonstrated that all the predicted load-extension curves of the proposed model follow closely to the measured ones up to the points of fracture initiation together with a good prediction on crack propagation in the vicinity of the bolt holes.* Consequently, the proposed model is demonstrated to be able to

predict accurately points of fracture initiation and their subsequent deformations, linking micro-mechanics to fracture assessment of structural members and bolted connections.

**Keywords:** High strength steel; True stress-strain curve; Bridgman's method; Post-necking behaviour;; In-situ X-ray CT scanning and imaging; Micro-void evolution; Numerical simulation; Ductile fracture; Shear fracture; Triaxiality; Lode angle; Fracture initiation and propagation; Bolted connection

## PUBLICATIONS

### *Journal papers*

- [J1] **M. Li**, H.-C. Ho, K.-F. Chung, and D. A. Nethercot, "Determination of true stress-strain curves of S355-S960 steels through modified Bridgman method," *Journal of Constructional Steel Research*, vol. 234, 2025.
- [J2] **M. Li**, H.-C. Ho, K.-F. Chung, and D. A. Nethercot, "In-situ X-ray CT investigation on micro-void evolution of structural steel towards verification on micro-fracture mechanics," *Construction and Building Materials*, under revision.
- [J3] **M. Li**, H.-C. Ho, K.-F. Chung, and D. A. Nethercot, "Investigation into ductile fracture behavior of S690 high strength steel under combined tension and shear actions," *Journal of Constructional Steel Research*, vol. 237, 2026.
- [J4] **M. Li**, H.-C. Ho, K.-F. Chung, and D. A. Nethercot, "From micro-mechanical calibration to macro-scale validation: ductile fracture modelling of S690 steel in bolted connections," In preparation.
- [J5] **M. Li**, H.-C. Ho, K.-F. Chung, and D. A. Nethercot, "Experimental and numerical investigation of fracture behaviour of S690 welded joints," In preparation.

*Conference papers*

- [C1] **M. Li**, H.-C. Ho, and K.-F. Chung. Determination of true stress-strain relationship of high strength S690 steel using Extended Bridgman's Method. PSSC 2023, Chengdu, China.
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# Chapter 1

## Introduction

### 1.1 Research background

With advancement of steel-making technology, high strength steels with yield strengths up to 690 N/mm<sup>2</sup>, i.e. S690 steel, have been applied widely in many mechanical structures, including heaving lifting equipment, and large machines, in the past twenty years. Superior mechanical properties of the S690 steel, especially their high yield strengths, allow design of structural members with reduced cross-sectional areas and self-weights. In recent years, such reductions are well sought after by structural engineers, and many of them would like to exploit these high strength steel in their construction projects to achieve savings in both construction cost and time, i.e. to reduce material consumption, and to allow easy handling and assembly in sites.

However, the use of these S690 steel introduces new challenges on safety of structures and buildings, particularly concerning fracture of these steel members and connections even under ductile failure modes. When compared with those commonly used normal strength S355 steel, the S690 steel are always considered to have a low ductility, i.e. small elongation limits at fracture. Many engineers have a concern that structural members and connections made of the S690 steel are more susceptible to ductile fracture, especially in connections and joints where highly localized deformations take place. It is widely believed that owing to the good ductility of the S355 steel, visible signs or warnings in structural members of the S355 steel undergoing large deformations will provide time for early intervention. However, it is uncertain whether the S690 steel is able to do that in a similar manner.

Even for the S355 steel, the risk of ductile fracture is particularly pronounced in connections, i.e. both bolted connections and welded joints, which are necessary in all steel structures. The presence of bolt holes always leads to stress concentrations in the steel plates, and this often causes early initiation of fracture. Hence, many engineers would like to determine whether the S690 steel are able to perform satisfactorily in buildings and structures.

In addition, a number of traditional ductile fracture models have not been validated to be applicable for the S690 steel, and many of the current constitutive models may not be able to simulate ductile fracture of the S690 steel, especially under complex states of stresses and strains. Since fracture usually occurs in the vicinity of the connections, understanding on both fracture initiation and propagation in these areas is crucial for an accurate prediction on the ductile fracture mechanism.

To tackle these challenges, the research presented in this thesis aims to investigate systematically the micro-void evolution and ductile fracture behaviour in the S690 high strength steel. By employing advanced experimental and numerical methods, this research seeks i) to develop the understanding of fracture mechanism, ii) to propose a ductile fracture model, and iii) to predict fracture initiation and propagation of bolted connections of the S690 steel. It is expected to develop new understanding on the ductile fracture mechanism of the S690 steel, and to develop rational guidelines and assessment methods to appraise ductile fracture behaviour of the S690 steel. Hence, a good understanding into the ductile fracture behaviour of the S690 steel is then established to confirm the presence of a high degree of structural safety, as permitted by the steel, is equally available, when compared with the S355 steel.

## 1.2 Objectives and scope of works

The primary objective of this research is to develop a comprehensive, experimentally validated ductile fracture model for the high strength S690 steel members and connections under complex states of stresses and strains. The scope of work includes:

5. **To develop a constitutive model** for the S690 steel by modifying the Bridgman method for precise determination of true stress-strain curves after allowing for non-uniform stress distributions.
6. **To study evolution of micro-voids of the S690 steel** using in-situ X-ray computed tomography (CT), and to examine void nucleation and growth up to fracture initiation, according to the commonly accepted Void Growth Model.
7. **To formulate a fracture initiation criterion for the S690 steel** that incorporates stress triaxiality and Lode angle effects to predict their ductile fracture under combined tension and shear actions.
8. **To validate the proposed fracture model** through structural testing of bolted connections of the S690 steel with various configurations to ensure applicability in structural design.

This project aims to bridge the gap between material-level fracture mechanisms and fracture behaviour of structural members and bolted connections, ultimately providing engineers with robust tools to assess structural performance and safety of the S690 steel structures.

### 1.3 Methodology

Both experimental and numerical methods are used in this project, and details are:

- *Task 1: Investigations into true stress-strain characteristics of S355 to S960 steels*

The true stress-strain characteristics of structural steels, i.e. S355, S460, S690 and S960, were investigated through tensile tests on cylindrical standard coupons, and the instantaneous dimensions ( $a_i$ ,  $R_i$ ) were precisely tracked through high-resolution digital imaging. Then, the numerical models were established to predict load-extension ( $N - \Delta$ ) curves of the coupons for direct comparison with measured data. These simulations allowed assessment on accuracy of various true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves obtained both the Bridgman and the Modified Bridgman methods. Comparison with predicted results obtained from the previously proposed Instantaneous Area Method (*iAreaM*) is also presented.

- *Task 2: Investigations into micro-void evolution of S355 to S690 steels*

In this task, the micro-void evolution in structural steel through the use of in-situ X-ray computed tomography (CT) was investigated, and specially designed steel coupons were loaded in displacement control to various elongations, and scanned simultaneously to capture formation or growth of micro-voids with a minimum volume of  $56 \mu\text{m}^3$  up to fracture. Three CT coupons of different steel grades, i.e. S355, S690 and S960 steel, were investigated to track key stages of micro-void evolution, i.e. nucleation, growth, and coalescence, through scanning and imaging at critical deformation points. High-resolution 3D reconstruction technique was then employed to conduct detailed morphological analysis of the micro-voids captured, and then, their sizes, locations and distributions in the vicinity of the necking region of the coupons were examined. Numerical models were then established to examine the influence of stress triaxiality on the development of

micro-voids.

- *Task 3: Investigations into ductile fracture behaviour of S690 steel*

In this task, five types of notched specimens with different levels of stress triaxialities and Lode parameters were designed to decouple their effects on the specimens. Tensile tests on a total of 34 notched specimens were conducted carefully to capture their deformations up to fracture. Examination on fractured specimens using scanning electron microscopy (SEM) was also conducted to identify microstructural differences between tension- and shear-dominated fracture. Advanced numerical modelling using three dimensional solid elements was then carried out to simulate structural behaviour of these notched specimens up to fracture, and critical states of both stresses and strains for each of these coupons at fracture were identified.

- *Task 4: Investigations into ductile fracture behaviour of S690 bolted connections*

In order to examine applicability of the proposed ductile fracture model to bolted connections of specific steel plates, it is necessary to calibrate various parameters of the model, including material-specific parameters, through a series of tensile tests on six different types of coupons of the steel plates. A total of 39 coupons were tested under tensile loadings, and then their fracture surfaces were observed under SEM to distinguish different failure modes. Then, an advanced finite element model of tested coupons was established to calibrate the proposed ductile fracture model. The numerical modeling was then conducted on bolted connections to predict various locations and deformations of fracture initiation. Finally, the paths of fracture propagation in these bolted connections during the whole loading process were simulated by deleting fractured elements identified by the proposed model. Comparisons between the numerical and the experimental data were also



conducted, and detailed data analysis and interpretation were also made.

## **1.4 Significance**

This research significantly advances the field of structural engineering by establishing an experimentally validated framework for predicting ductile fracture in the S690 steel. The development of the Modified Bridgman method provides precise determination of true stress-strain curves for the S690 steels. **Through in-situ X-ray computed tomography (CT) scanning and loading, typical evolution of micro-voids in three different steels has been successfully examined, and the total void volumes at the points of fracture initiation are found to be too small to have any effect on the structural behaviour of the steels. Hence, the true stress-strain curves derived above are readily applicable up to fracture initiation without the need to consider the presence of any void in the steel. This research provides a well calibrated ductile fracture model to predict both the locations and the deformations of bolted connections between the S690 steel plates. This contributes to a safe design of the S690 steel members and connections, as it mitigates premature fracture, and supports a broad adoption of the high strength steel in cost-effective, lightweight, and resilient structural systems.**

## 1.5 Outline of thesis

- *Chapter 1: Introduction*

This chapter provides an introduction to the research background of this thesis. Key objectives, the research methodology, and the significance of this project are also presented.

- *Chapter 2: Literature review*

This chapter presents an extensive review on key research topics of high strength steel, including determination of true stress-strain relationships of steels, ductile fracture mechanism and models, and advanced measurement techniques in mechanical testing.

- *Chapter 3: Determination of true stress-strain curves of S355-S960 steels through modified Bridgman method*

This chapter presents a comprehensive investigation into the true stress-strain relationships of S355 to S960 structural steels using experimental and numerical approaches. Standard tensile tests were conducted on cylindrical standard coupons of four steel grades, and advanced digital imaging techniques were employed to capture precise geometric parameters,  $a$  and  $R$ , of the necking region. A Modified Bridgman Method was proposed to simplify determination of the spatial necking radii ratio  $a/R$ , and its accuracy was validated through finite element simulations, and also comparisons with test data. In addition, a comparison with numerical data obtained from the Instantaneous Area Method (*iAreaM*) previously proposed is found to be highly satisfactory.

- *Chapter 4: In-situ X-ray CT investigation on micro-void evolution of structural steel towards verification on micro-fracture mechanics*

This chapter used *in-situ* X-ray CT technique to track micro-void evolution during

tensile testing of S355, S690, and S960 steel coupons. The detailed morphological analysis of the captured micro-voids was conducted by using the high-resolution 3D reconstruction technique, and then, the sizes, the locations and the distributions of micro-voids in the vicinity of the necking region of the coupons were examined. Numerical analysis was conducted, and the results confirmed void growth dependence on stress triaxiality and equivalent plastic strain, validating the Rice-Tracey model. Availability of CT-measured void data also enabled a calibration of GTN model parameters for ductile fracture prediction, highlighting importance of critical stress states after necking.

- *Chapter 5: Ductile fracture behavior of S690 high strength steel under combined tension and shear actions*

In this chapter, the effects of combined tension and shear actions on the ductile fracture behaviour of the S690 steel were investigated through experimental and numerical approaches. Tensile tests on a total of 34 specially designed notched coupons and specimens were conducted to capture their deformations up to fracture. Advanced numerical modelling using three dimensional solid elements was then carried out to simulate structural behaviour of these notched coupons and specimens up to fracture. Five different modern fracture models were calibrated against the numerical results, and their capabilities in capturing the fracture behaviour of the S690 steel were also examined. Based on the numerical models and a wide range of the Lode parameters obtained above, a new ductile fracture model is proposed and verified for the S690 steel under combined tension and shear actions.

- *Chapter 6: Ductile fracture modelling of S690 steel in bolted connections*

This chapter calibrates and validates the ductile fracture model proposed in Chapter

5 to predict fracture initiation and crack propagation in a total of six bolted connections between the S690 steel plates. Six types of specially designed notched coupons were designed and tested under tension to generate various stress states. The fracture model parameters were then calibrated through advanced numerical simulations. It is demonstrated that the calibrated model is able to predict both locations and deformations of fracture initiation in bolted connections under monotonic loading. Finally, the paths of crack propagation within the bolted connections were also shown to be simulated successfully during loading by removing elements identified as being fractured according to the proposed fracture criterion.

- *Chapter 7: Conclusions and future work*

This chapter summarizes key findings of the project together with their academic merits. A plan for future work is also presented.

## **Chapter 2**

### **Literature review**

#### **2.1 Introduction**

Ductile fracture behaviour represents a critical failure mode in structural steel components. It significantly influences the safety, reliability, and design methodologies of modern structure. Understanding the mechanisms governing ductile fracture initiation and propagation is essential for advancing numerical simulations and ensuring structural safety under extreme loading conditions. This chapter comprehensively reviews relevant research on ductile fracture of structural steels, and consists of three key parts as follows:

- 1) True Stress-Strain Characterization (Chapter 2.2): This section examines different methods for deriving accurate post-necking constitutive relationships, with particular attention paid to the limitations of traditional empirical corrections such as Bridgman's formula as well as recent progress in iterative and numerical strategies.
- 2) Ductile Fracture Models (Chapter 2.3): In this section, the key parameters governing the ductile fracture behaviour are discussed, including the stress triaxiality and the Lode angle. Then both uncoupled and ductile fracture models are critically evaluated, alongside numerical modelling techniques for simulating crack initiation and propagation.
- 3) Advanced Measurement Techniques (Chapter 2.4): Recent developments in high-resolution measurement techniques, i.e. Digital Image Correlation (DIC) and X-ray Computed Tomography (CT), are reviewed for their effectiveness in facilitating

accuracy observation and validation of both deformation and fracture processes at various scales.

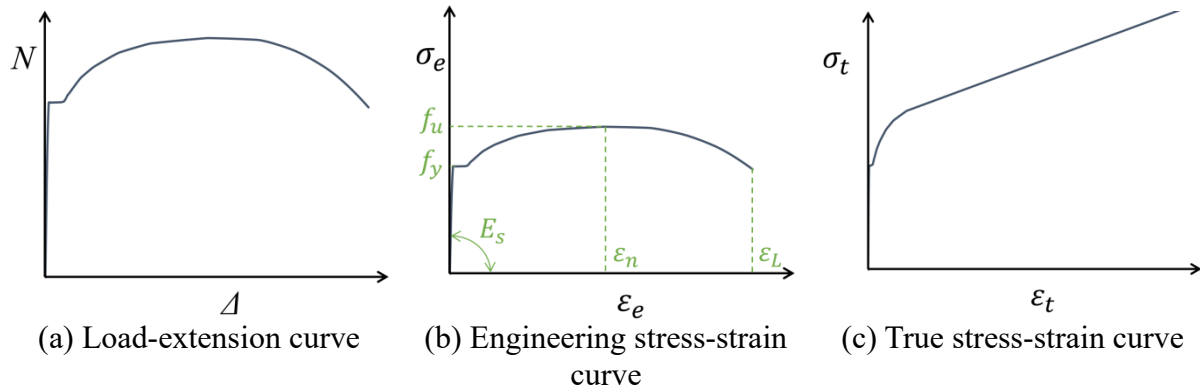
## 2.2 True stress-strain relationship

In structural mechanics, an accurate determination of “stresses” and “strains” in structural members and joints is essential for their structural analyses and numerical simulations. For many years, tensile tests have been conducted on cylindrical coupons of structural steel to provide data to describe their basic mechanical responses, i.e. an applied load – extension ( $N - \Delta$ ) curves, as shown in [Figure 2.1a](#)). Each of these curves is readily transformed into an engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curve of steel which is typically made up of a linear and elastic portion, and then a non-linear and plastic portion, as shown in [Figure 2.1b](#)). Key parameters of the ( $\sigma_e - \epsilon_e$ ) curve include

- i) the slope of the linear and elastic portion, which is commonly referred as the Young’s modulus,  $E_s$ ;
- ii) the maximum applied stress of the linear and elastic portion, which is commonly referred as the yield strength,  $f_y$ ;
- iii) the maximum applied stress of the non-linear and plastic portion, which is commonly referred as the tensile strength,  $f_u$ ; and
- iv) the maximum elongation of the non-linear and plastic portion, which is commonly referred as the elongation limit,  $\epsilon_L$ .

It should be noted that when the tensile strength of the coupon is reached, necking takes place at a critical cross-section of the coupon with the minimum cross-sectional area at which the cross-sectional distributions of both stresses and strains cease to be uniform. As the elongation increases, the cross-sectional area of the necking region  $A_i$  is reduced significantly, when compared with its original value  $A_0$ . Hence, it becomes necessary

to perform data analysis based on reduced dimensions of the coupon, i.e. to derive true stress-strain ( $\sigma_t - \varepsilon_t$ ) curve, as shown in **Figure 2.1c**).



**Figure 2.1** Characteristic curves of standard tensile tests.

The study on non-uniform distributions of both stresses and strains across the necking region of cylindrical metallic coupons under tension may be traced back to the 1950's. According to the results of tensile tests on a large number of metallic coupons, Bridgman proposed a widely adopted formula to determine true stress-strain characteristics of these coupons through the ratio of  $a/R$  where  $a$  is the cross-sectional radius and  $l/R$  is the longitudinal curvature of the necking region of the coupons during tests. As it was often difficult to measure both  $a$  and  $l/R$  accurately, an empirical expression was also proposed to determine the ratio  $a/R$ . However, accuracy of the stresses predicted by the Bridgman method based on the empirical expression at large deformations after necking is somehow uncertain.

Owing to advances in steel-making technology over the past two decades, high strength steel with yield strengths  $f_y$  at 690 and 960 N/mm<sup>2</sup> are readily available in many parts of the world. While the Young's modulus  $E_s$  of these high strength steel remains to be the same as the normal strength steel, their elongation limits  $\varepsilon_L$  are found to be smaller than those of the normal strength steel. Hence, there is a need to examine the deformation characteristics of these high strength steel, in particular, their engineering

stress-strain ( $\sigma_e - \varepsilon_e$ ) curves, and also their true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves for engineering applications.

### 2.2.1 Derivation of true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves

In general, there are two different approaches to correct the measured values of the direct stresses at the center of the minimum cross-section, i.e. the necking region, to derive true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves, and they are i) *a direct correction approach* based on formulations, and ii) *an iterative correction approach* based on numerical simulations. These two approaches provide corrections to both average stresses  $\sigma_{av}$  and average strains  $\varepsilon_{av}$  to give the true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves. The direct correction approach modifies the measured values of the engineering stresses  $\sigma_e$  into the true stresses  $\sigma_t$  through analytical formulations or empirical expressions after approximations. The most representative analytical solution is the Bridgman's method [1]. On the contrary, the iterative correction approach gives corrections based on a combination of measured and numerical results, and a converged solution is obtained after a number of iterations.

### 2.2.2 Direct correction approach

Nowadays, through data analysis on digital images on a standard coupon captured during a tensile test, both the initial radius,  $a_0$ , and the instantaneous radius,  $a_i$  of the coupon under an applied force  $N$  may be determined readily. Hence, both the average stress  $\sigma_{av}$  and the average strains  $\varepsilon_{av}$  are given as follows:

$$\sigma_{av} = \frac{N}{\pi a_i^2} \quad (2-1)$$

$$\varepsilon_{av} = 2 \ln \left( \frac{a_0}{a_i} \right) \quad (2-2)$$



It should be noted that the stress state of the central section of the coupon changes from a uni-axial state to a tri-axial state after necking, and both the average stress  $\sigma_{av}$  and the average strains  $\varepsilon_{av}$  based on the uni-axial calculation method should be modified into the true stress  $\sigma_t$  and the true strain  $\varepsilon_t$  corresponding to the tri-axial stress state. The relationship between these two types of stresses in different stress states is as follows:

$$\varepsilon_t = \eta_\varepsilon \cdot \varepsilon_{av} \quad (2-3)$$

$$\sigma_t = \eta_\sigma \cdot \sigma_{av} \quad (2-4)$$

where  $\eta_\varepsilon$  is the strain correction factor, and  $\eta_\sigma$  is the stress correction factor.

Most methods of direct correction approach only deal with the problem of a non-uniform distribution of stresses in a necking region and give true stresses according to various assumptions. They all rely on the geometrical dimensions of the necking region of the coupon, i.e. the radius of the minimum cross section,  $a$ , and the in-plane curvature  $l/R$  along the longitudinal direction. It should be noted that these methods ignore the non-uniform distribution of strains across the cross section of the necking region.

In order to determine the true stresses of the necking region, Bridgman [1] proposed an empirical expression to determine the stress modification factor using the  $a/R$  ratio with an assumption that the shape of the necking region was part of a circle with its in-plane radius  $R$ . Other researchers also derived similar stress modification factors based on the  $a/R$  ratio. Siebel [2] and Chen [3] used different equation to approximate the in-plane radius  $R$ . However, all these researchers found that measuring the values of  $R$  was somehow difficult. In order to avoid such measurements, Mirone [4] proposed an empirical modification formula in his MLR model which was based on a relationship between the ratio of the true stress  $\sigma_t$  to the average stress  $\sigma_{av}$ , and also the difference

between the equivalent strain  $\varepsilon_{eq}$  and the necking strain  $\varepsilon_n$  of a large number of tensile tests on various metal coupons. **Table 2-1** summarizes various stress modification expressions proposed by these researchers.

In addition to the Bridgman's Method, there are many models which are based on the power law function. A notable contribution by Y. Ling [5] introduced a weighted average approach that established an empirical lower bound solution through a power law function, and a linear upper bound constraint for characterizing the post-necking stress-strain behaviour. This approach modified the true stress-strain curve through the use of a constant weighting parameter that proportionally combined these two boundaries across the entire plastic deformation range.

Recently, Zhou et al. [6] proposed the GPN model in which the power law function was further developed to evaluate the true stresses according to the hardening modulus of the true stress-strain curve. All the independent parameters in this model were calibrated through trials and errors. Then, a set of empirical expressions were established for these independent parameters according to normalized post-necking true stress-strain curves. As a result, the true stress-strain curves for a wide range of structural steels were readily formulated.

**Table 2-1** Stress modification formulae for determination of true stresses.

| Researcher   | Stress modification formulae                                                                                                                                                                        |
|--------------|-----------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Bridgman [1] | $\eta_{\sigma}^B = \left[ \left(1 + \frac{2R}{a}\right) \cdot \ln \left(1 + \frac{a}{2R}\right) \right]^{-1}$                                                                                       |
| Siebel [2]   | $\eta_{\sigma}^S = \left[ 1 + \frac{a}{4R} \right]^{-1}$                                                                                                                                            |
| Chen [3]     | $\eta_{\sigma}^C = 2 \left[ 1 + \left(1 + \frac{R}{a}\right) \cdot \ln \left(1 + \frac{a}{R}\right) \right]^{-1}$                                                                                   |
| Mirone [4]   | $\text{MLR}(\varepsilon_{av} - \varepsilon_n) =$<br>$1 - 0.6058(\varepsilon_{av} - \varepsilon_n)^2 + 0.6317(\varepsilon_{av} - \varepsilon_n)^3$<br>$- 0.2107(\varepsilon_{av} - \varepsilon_n)^4$ |

### 2.2.3 Iterative correction approach

In recent years, a number of methods based on this approach are reported in the literature. Zhang et al. [7] used the Deshpande-Fleck hardening model to characterize the hardening curve of aluminum foam material. The parameters of the hardening model under a high straining rate were obtained from a combined optimization strategy of a continuous artificial neural network model and a particle swarm optimization algorithm. Based on a number of measured data of uni-directional tensile tests of 304L stainless steel, Knysh et al. [8] used the combined optimization strategy of the response surface method and Modified Cors optimization algorithm to obtain the hardening curve with a large tensile strain range in reverse. Reis et al. [9] parameterized the hardening curve based on the Swift hardening model and finally determined the hardening curve under a large strain range through the iterative method of hardening model parameters. In addition, Aguir et al. [10], Malakizadi et al. [11], Chamekh et al. [12], and Prates et al. [13] also used such methods to carry out studies to obtain hardening model parameters of materials. However, these methods required complex iterative processes and lacked theoretical basis.

Ho et al. [14] first proposed the Instantaneous Area Method, or the *iArea* method, in 2019. Through the use of digital imaging and processing technique, deformations of rectangular coupons of thin S355 and S690 steel plates under monotonic tension were captured with a high-resolution digital camera, and the engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curves were obtained with a high level of accuracy. Then, finite element models with solid elements were adopted to simulate these tensile tests, and to examine both the stress and the strain distributions of these coupons at large deformations after necking. By analyzing the differences between the maximum and the average tensile stresses within the coupons along various stages of deformation, a series of stress and strain

modification factors throughout the entire deformation ranges were obtained. Hence, the true stress-strain ( $\sigma_t$  -  $\varepsilon_t$ ) curves of these coupons were readily obtained after successive approximations. It was shown that convergence in these true stress-strain ( $\sigma_t$  -  $\varepsilon_t$ ) curves were often obtained within five approximations. In addition, Ho et al. [15] applied the Method to analyze test results of 30 cylindrical coupons of S690 steel in standard tensile tests, and an efficient constitutive model for the S690 steel was achieved after a careful calibration against test data.

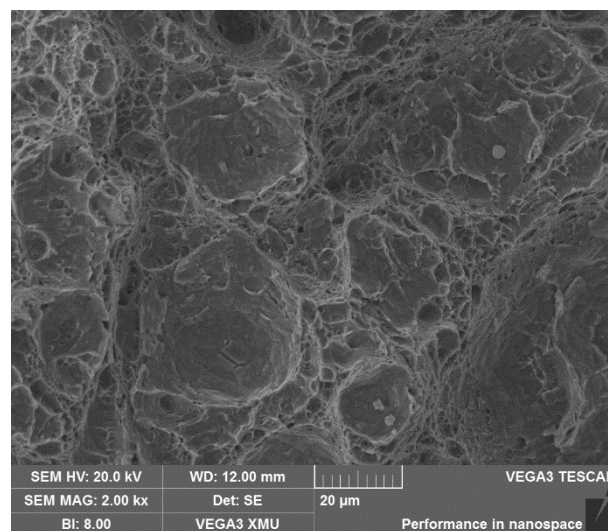
#### **2.2.4 Other approaches**

There is an increasing research trend reported in the literature to examine the necking of non-circular cross-sections under tension. Analytical and numerical models become available to account for the influence of cross-sectional shapes of the coupons, e.g., circular or rectangular cross-sections, on the triaxial stress state during necking. These models have improved accuracy of a number of post-necking constitutive models, and expanded their applicability to a wide variety of geometry and loading conditions [14, 16]. Recent advancements in these post-necking analyses also include application of machine learning (ML) techniques to correct true stress-strain curves after necking. Beltramo et al. [17] has developed an inverse identification framework that combines numerical simulations with machine learning algorithms to calibrate hardening laws using necking profiles to achieve a high accuracy in stress prediction. Similarly, Lee et al. [16] have inversely identified the constitutive model by tracking the measured load–displacement curves through repeated numerical analyses using an optimization algorithm.

## 2.3 Ductile fracture models

### 2.3.1 Microscopic mechanism of ductile fracture

The microscopic mechanism provides a foundation for establishing ductile fracture models. Nowadays, the most acceptable theory of the micro mechanism behind ductile fracture is the void growth theory, which defines three stages for the micro void inside the material: void nucleation, growth and coalescence. Void nucleation is the first step of forming ductile fracture. The main reason for void nucleation is the separation of the base material and the inclusion particles. These particles are typically carbides and sulfides which form as inclusions between the grains of the steel matrix. After the nucleation, micro voids will grow with the increase of hydrostatic pressure and plastic strain. When voids expand to the critical volume, they will coalesce with adjacent ones and form the macro crack. The historical understanding of this mechanism arises from micrographs of the fracture surface, in which dimples and inclusions are visible, as shown in **Figure 2.2**.



**Figure 2.2** Scanning electron micrograph of ductile fracture surface

However, the void growth theory has several deficiencies. First, there is no physical

evidence of void nucleation, and the critical value of void volume when fracture cannot be quantified. Second, the correlation between the void volume ratio and its influence on both strength and stiffness of the material has not been quantified clearly. Finally, no micrographic evidence of fracture initiation induced by void coalescence exists. Therefore, the micro mechanism of ductile fracture and fracture models based on the void growth theory needs further discussion.

### 2.3.2 Parameters for describing stress state

#### 1. Stress Triaxiality

The stress triaxiality ( $T$ ) plays an important role in ductile fracture of materials. As a measurement of the stress concentration, it is defined as the ratio of the hydrostatic stress ( $\sigma_m$ ) to the von-Mises stress ( $\sigma_{eq}$ ):

$$T = \frac{\sigma_m}{\sigma_{eq}} \quad (2-5)$$

The hydrostatic stress and the von-Mises stress can be calculated by equations below:

$$\sigma_m = \frac{\sigma_1 + \sigma_2 + \sigma_3}{3} \quad (2-6)$$

$$\sigma_{eq} = \sqrt{\frac{1}{2}((\sigma_1 - \sigma_2)^2 + (\sigma_1 - \sigma_3)^2 + (\sigma_2 - \sigma_3)^2)} \quad (2-7)$$

where  $\sigma_1$ ,  $\sigma_2$  and  $\sigma_3$  are three principal stresses. The sign of the stress triaxiality is determined by the hydrostatic stress, which is positive for tension and negative for compression.

#### 2. Equivalent plastic strain

Equivalent plastic strain describes the extent of plastic deformation. It is an invariant of

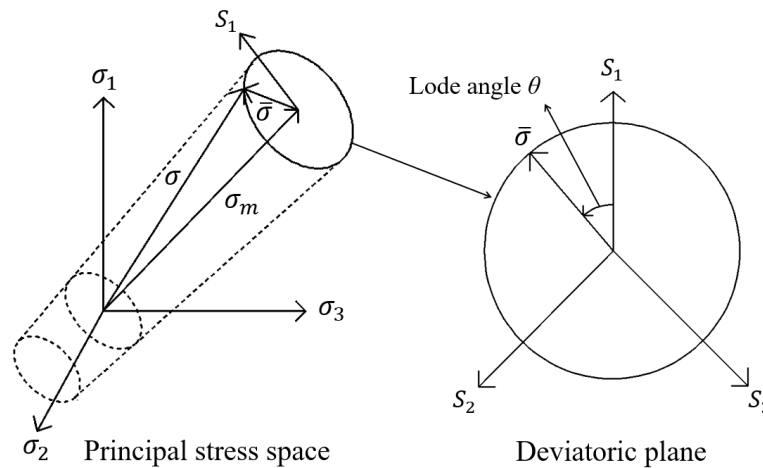
the plastic strain tensor, representing its total cumulant, the increment form of which can be expressed:

$$d\varepsilon_p = \sqrt{\frac{2}{3} d\boldsymbol{\varepsilon}_p^{ij} \cdot d\boldsymbol{\varepsilon}_p^{ij}} \quad (2-8)$$

The equivalent plastic strain is a strong predictor of ductile fracture of materials, based on which many criteria have been established.

### 3. Lode angle

The Lode angle describes the deviatoric stress state, which may affect the ductile fracture of materials at low stress triaxiality regions. In the principal stress space, the plane perpendicular to the hydrostatic pressure denotes the deviatoric plane, and the Lode angle  $\theta$  is defined as the angle between the principal deviatoric stress axis and the projection of the stress on the deviatoric plane, as shown in **Figure 2.3**. The Lode angle ranges from  $-\pi/3$  to  $\pi/3$ , and  $\theta = 0$  represents the pure shear state.



**Figure 2.3** Definition of the Lode angle

#### 4. Lode angle parameter

Considering the periodic nature of the Lode angle, a normalized version is presented in the following equation:

$$\bar{\theta} = 1 - \frac{6\theta}{\pi} \quad (2-9)$$

The parameter ( $\bar{\theta}$ ) will be referred to as the ‘Lode angle parameter’. Its value ranges from -1 to 1. The lode angle parameter equals 1 under the condition of axial tension and equals -1 under axial compression. When  $\bar{\theta}$  equals 0, the stress state may be pure shear or plane strain state.

#### 5. Lode parameter

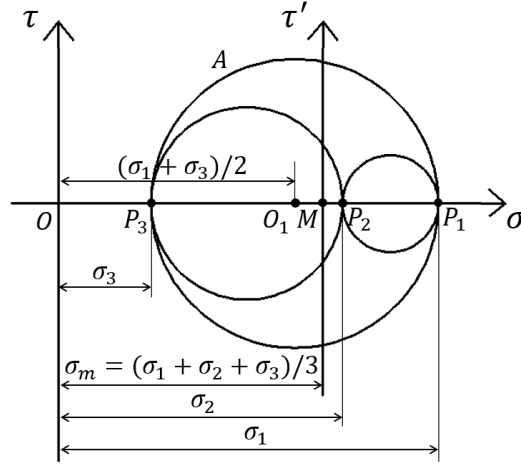
The Lode parameter ( $L$ ) is an alternative variable to describe the state of deviatoric stresses. A stress circle is shown in [Figure 2.4](#), where  $OP_1 = \sigma_1$ ,  $OP_2 = \sigma_2$  and  $OP_3 = \sigma_3$ . The circles with diameters of  $P_1P_3$  are termed circle A. Then, taking the distance of  $OM$  as  $\sigma_m$ ,  $\tau'$  axis is made through point  $M$ . Therefore,

$$MP_1 = \sigma_1 - \sigma_m = s_1 \quad (2-10)$$

$$MP_2 = \sigma_2 - \sigma_m = s_2 \quad (2-11)$$

$$MP_3 = \sigma_3 - \sigma_m = s_3 \quad (2-12)$$





**Figure 2.4** Definition of Lode parameter

The center of Circle A is taken as  $O_1$ . The Lode parameter is defined as the ratio of  $O_1P_2$  to  $O_1P_1$ , representing the relative position of  $P_1$ ,  $P_2$  and  $P_3$ , which is the characteristic of the stress state. The Lode parameter can be calculated by the following equation and ranges from -1 to 1.

$$L = \frac{O_1P_2}{O_1P_1} = \frac{OP_2 - OO_1}{O_1P_1} = \frac{\sigma_2 - (\sigma_1 + \sigma_3)/2}{(\sigma_1 - \sigma_3)/2} = \frac{2\sigma_2 - \sigma_1 - \sigma_3}{\sigma_1 - \sigma_3} \quad (2-13)$$

For the monotonic tension stress state,  $\sigma_1 > 0$ ,  $\sigma_2 = \sigma_3 = 0$  and  $L = -1$ ; for the pure shear stress state,  $\sigma_2 = 0$ ,  $\sigma_1 = -\sigma_3$  and  $L = 0$ ; for the plane stress state,  $2\sigma_2 = \sigma_1 + \sigma_3$  and  $L = 0$ .

The Lode parameter can also be expressed by Lode angle:

$$L = \sqrt{3}\tan\left(\theta - \frac{\pi}{6}\right) \quad (2-14)$$

### **2.3.3 Models for fracture initiation:**

#### **1. Uncoupled model**

The characteristic of the uncoupled models is that the constitutive model does not consider the damage of the material before the ductile fracture occurs. The mechanical properties of the material are described by the classical elastic-plastic constitutive model, and some macroscopic external variables (such as stress and strain) are used to determine the fracture initiation state. With fewer material parameters, uncoupled models are easy to be calibrated. In the late 1960s, Rice and Tracey [18] found that the micro-voids' growth depends on the hydrostatic pressure's ratio to yield stress. They deduced the relationship between the void growth rate and the plastic strain for the situation of high stress triaxiality. Then d'Escatha and Devaux [19] replaced the yield stress with von Mises stress in the Rice-Tracey model. Kanvinde [20] rewrote this model and formed a practical failure criterion – void growth model (VGM). Hancock and Mackenzie [21] simplified d'Escatha and Devaux's equation by ignoring the variation of the stress triaxiality during the loading process and formed the stress-modified critical strain (SMCS) model. All the above models were based on the void nucleation, growth and coalescence theory, and only considered the single void, ignoring the interaction among voids. In 1985, Johnson and Cook [22] proposed an empirical model based on experimental results using stress triaxiality, temperature and strain rate. Although the Johnson-Cook model and SMCS model are derived from different methodologies, they share the same expression.

In 2004, Bao and Wierzbicki [23] found that stress triaxiality is not the only parameter controlling the ductile fracture of materials under pure shear states. For low stress triaxiality situations, deviatoric stresses, which can be measured by the Lode angle or the Lode parameter, also have considerable effects on the ductile fracture of materials.

Many researchers have proposed different micromechanical models considering the effect of the Lode angle or the Lode parameter for the prediction of ductile fracture of materials under shear stresses. Xue and Wierzbicki [24] combined fracture loci of axial stress and plane-strain state, and considered the Lode angle by an elliptic function. Bai and Wierzbicki [25] defined fracture locus by considering three kinds of stress states: compression, tension and shear, and combined them with the Lode angle parameter by a quadratic function. Then they modified the classic Mohr-Coulomb fracture criterion by transferring it to the three-dimensional space of ‘stress triaxiality, Lode angle parameter and equivalent plastic strain [26]. In recent years, Lou and Huh [27] considered that shear fracture was controlled by the normalized maximal stress. Wen and Mahmoud [28] extended the void growth model to low triaxiality situations by adding a Lode angle multiplier. Danas and Ponte Castañeda [29], and Kiran and Khandelwal [30] described the evolution of micro voids in materials under shear stresses as a successive procedure of nucleation, growth, elongation and collapse, which would result in the localized shear bands. Yin et al. [31] propose a piecewise model, and the fracture equivalent plastic strains caused by tension and shear are calculated separately. However, those fracture models were proposed based on alloy or normal strength steels, so their accuracy in high strength steel needs further verification.

The ductile fracture models mentioned above are listed in [Table 2-2](#). It should be noted that all the deviatoric stress-related parameters are transferred to the Lode angle parameter ( $\bar{\theta}$ ) or Lode parameter ( $L$ ) mentioned above.

**Table 2-2** Uncoupled ductile fracture models

| Fracture model                               | Expression                                                                                                                                                                                                                                             |
|----------------------------------------------|--------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------------|
| Rice-Tracey model                            | $\ln\left(\frac{R}{R_0}\right) = 0.283 \int_0^{\varepsilon_p} \exp\left(\frac{1.5\sigma_m}{\sigma_y}\right) \cdot d\varepsilon_p$                                                                                                                      |
| Void growth model (VGM)                      | $VGI = \int_0^{\varepsilon_p} \exp(1.5T) \cdot d\varepsilon_p > \eta$                                                                                                                                                                                  |
| Stress modified critical strain model (SMCS) | SMCS Index = $\exp(1.5T) \cdot \varepsilon_p > \alpha$                                                                                                                                                                                                 |
| Johnson-Cook model                           | $\varepsilon_p^{critical} = C_1 + C_2 \exp(C_3 T)$                                                                                                                                                                                                     |
| Xue-Wierzbicki model                         | $\varepsilon_f = d_1 \exp(-d_2 T) - [d_1 \exp(-d_2 T) - d_3 \exp(-d_4 T)] \cdot \left(1 - \left \cos\left(\frac{\pi}{2}(1 - \bar{\theta})\right)\right ^m\right)^{\frac{1}{m}}$                                                                        |
| Bai-Wierzbicki model                         | $\varepsilon_p^{critical} = \left[\frac{1}{2}(D_1 \exp(-D_2 T) + D_5 \exp(-D_6 T)) - D_3 \exp(-D_4 T)\right] \bar{\theta}^2 + \frac{1}{2}(D_1 \exp(-D_2 T) - D_5 \exp(-D_6 T)) \bar{\theta} + D_3 \exp(-D_4 T)$                                        |
| Modified Mohr-Coulomb model                  | $\varepsilon_p^{critical} = \left(\frac{A}{c_2} \left[ \sqrt{\frac{1+c_1^2}{3}} \cos\left(\frac{\bar{\theta}\pi}{6}\right) + c_1 \left(T + \frac{1}{3} \sin\left(\frac{\bar{\theta}\pi}{6}\right)\right) \right]\right)^{\frac{1}{n}}$                 |
| Lou-Huh model                                | $D(\varepsilon_p) = \frac{1}{g_3} \int_0^{\varepsilon_p} \left(\frac{2}{\sqrt{L^2 + 3}}\right)^{g_1} \left(\frac{1+3T}{2}\right)^{g_2} \cdot d\varepsilon_p > 1$                                                                                       |
| Wen-Mahmoud model                            | $\varepsilon_p^{critical} = h_1 \exp(h_2 T) \left[\cos\left(\frac{\bar{\theta}\pi}{6}\right)\right]^{h_3} \left[1 + h_4 \left(\frac{\sin\left[\frac{\pi}{6}(1 - \bar{\theta})\right]}{\cos\left(\frac{\bar{\theta}\pi}{6}\right)} - 0.5\right)\right]$ |
| Yin's model                                  | $\varepsilon_p^{critical} = \min\{\varepsilon_p^t, \varepsilon_p^s\}, \begin{cases} \varepsilon_p^t = \alpha \cdot \exp(-1.5T) \\ \varepsilon_p^s = \frac{q \cdot \exp[(p_1 T - p_2 L) \cdot q_2]}{(T + 1)^{\frac{1}{n} q_1}} \end{cases}$             |

The physical meanings of parameters in the above models are as follows:  $\varepsilon_p^{critical}$  is the critical equivalent plastic strain;  $n$  is the hardening exponent;  $R$  is the final void diameter;  $R_0$  is the initial void diameter;  $\eta$ ,  $\alpha$ ,  $C_1 - C_3$ ,  $D_1 - D_6$ ,  $A$ ,  $c_1 - c_3$ ,  $h_1 - h_4$ ,  $C_1 - C_3$ ,  $p_1$ ,  $p_2$ ,  $q$ ,  $q_1$ ,  $q_2$  are material parameters.

## 2. Coupled model

In coupled models, a damage index (such as the void volume fraction) is introduced into the constitutive relationship of material. The coupled model usually has more material parameters coupled with the constitutive equation and is difficult to calibrate. Representative coupled models are the continuous damage model proposed by Lemaitre [32] and the GTN model [33].

Gurson analyzed the growth process of cylindrical and spherical micro voids in finite solid cells, deduced the plastic yield surface by considering the influence of void volume fraction and hydrostatic stress, and proposed the porous plastic damage model [34]. Tvergaard [35, 36] introduced two adjustment parameters to consider the interaction between voids; Needleman and Tvergaard [33] took into account the material's rapid loss of carrying capacity caused by void coalesces in Gurson's model and finally formed the GTN model.

The void volume fraction is calculated as the ratio of the void volume to the total volume of the material. The yield function of the GTN model is shown below:

$$\Phi = \left( \frac{\sigma_{eq}}{\sigma_y} \right)^2 + 2q_1 f^* \cosh \left( \frac{3q_2 \sigma_m}{2\sigma_y} \right) - (1 + q_3 f^{*2}) \quad (2-15)$$

where  $q_1$ ,  $q_2$  and  $q_3$  are correction parameters that take into account the effect of the void interaction [37];  $f^*$  is a damage variable that is a function of the void volume fraction  $f$ :

$$f^* = \begin{cases} f & f_0 \leq f \leq f_c \\ f_c + k(f - f_c) & f_c < f \leq f_F \\ f_u^* & f > f_F \end{cases} \quad (2-16)$$

where the parameter  $k = \frac{(f_u^* - f_c)}{(f_F - f_c)}$ ;  $f_0$  is the initial void volume fraction;  $f_c$  is the critical void volume fraction for void coalescence;  $f_F$  is the void volume fraction when the

material point fails;  $f_u^* = 1/q_1$  denotes the void volume fraction when stress equals zero.

When  $f$  reaches  $f_F$ , the material is assumed to lose the stress carrying capacity completely.

Before the void coalescence, the variation of void volume fraction is due to the growth of existing voids  $df_G$  and the nucleation of new voids  $df_N$ , which can be expressed as:

$$df = df_G + df_N \quad (2-17)$$

Base on the incompressibility hypothesis:

$$df_G = (1 - f)d\varepsilon_p : I \quad (2-18)$$

where  $d\varepsilon_p$  is the plastic strain rate tensor;  $I$  is the second order unit tensor. The main form of new hole initiation is the fracture or debonding of matrix particles at the interface. The strain-controlled hole initiation criterion can be expressed as:

$$df_N = \frac{f_n}{s_n \sqrt{2\pi}} \exp \left[ -\frac{1}{2} \left( \frac{\varepsilon_p - \varepsilon_n}{s_n} \right)^2 \right] d\varepsilon_p \quad (2-19)$$

where  $f_n$  is the volume fraction of inclusions from which new voids can be nucleated,  $\varepsilon_n$  is the average equivalent plastic strain when new voids nucleate and  $s_n$  is the standard deviation of nucleation strain.

For steel,  $f_0$  typically ranges from 0.001 to 0.005 due to the presence of initial defects, while  $f_c$  and  $f_f$  range from 0.05 to 0.15, and from 0.05 to 0.2, respectively, after careful calibration against relevant test data over a practical range of triaxiality [38, 39]. It should be noted that these models are equally applicable to other metals, such as aluminium alloys, and the corresponding values of  $f_0$  are found to range from 0.0005 to 0.002, due to fewer inclusions. The corresponding values of  $f_c$  and  $f_f$  are found to range

from 0.1 to 0.2, and from 0.2 to 0.3 respectively, due to enhanced ductility and anisotropic void growth [40]. The values of  $f_N$  are found to range from 0.01 to 0.03 for steel, and from 0.005 to 0.015 for aluminium alloys [41, 42]. It should be noted that advanced measurement technique, such as X-ray tomography, have been employed to investigate ductile fracture of various steel [43], and the values of these parameters had been obtained indirectly through a systematic analysis on void evolution. **Table 2-3** summarizes these void-related parameters in GTN model for structural steel [38, 44-47].

**Table 2-3** Parameters for GTN model available in the literature

| Steel grade | Initial void fraction<br>$f_0$ (%) | Void nucleation fraction<br>$f_N$ (%) | Critical void fraction<br>$f_c$ (%) | Failure void fraction<br>$f_t$ (%) | Reference |
|-------------|------------------------------------|---------------------------------------|-------------------------------------|------------------------------------|-----------|
| S235        | 0.0017                             | 0.04                                  | 0.06                                | 0.667                              | [44]      |
| Q345        | 0.0050                             | 0.04                                  | 0.25                                | 0.28                               | [45]      |
| Q355        | 0.002                              | 0.034                                 | 0.12                                | 0.17                               | [46]      |
| Q460        | 0.001                              | 0.003                                 | -                                   | 0.45                               | [47]      |
| S700        | 0.001                              | -                                     | 0.008                               | 0.055                              | [38]      |

Apart from the GTN model, another approach of the coupled model is the continuous damage model (CDM), which directly reduces the stress or Young's modulus in the constitutive model by coupling the damage factors. In 1958, Kachanov [48] built the basic framework of continuous damage mechanics and first proposed using damage factor  $D$  to evaluate material damage. In 1985, Lemaitre [32] proposed a continuous damage model based on the concept of effective stress and the principle of thermodynamics. Then, this model has been widely used in predicting component fracture in the engineering field [49-51]. In 2015, Kang et al. [52] proposed a continuous damage model with three stages and two parameters and used it to simulate steel's ductile fracture process accurately. **Figure 2.5** illustrates the constitutive relationship of the three-stage and two-parameter model, in which  $DI$  determines when the damage initiates, and  $d$  controls the damage evolution. The first stage is the elastic

stage (O→A). When reaching the yield stress, the first stage ends (point A), and  $DI$  begins to accumulate with the increase of the equivalent plastic strain. At the second stage (A→B), the material reaches the damage initiation criterion ( $DI = 1$ ). The third stage is the softening stage (B→C). At the beginning of this stage (point B), the damage starts to accumulate, and the stress and Young's modulus decrease. When  $d$  reaches 1, the stress reduces to 0, which means the material occurs fracture, and the strain in point C is the critical equivalent plastic strain. The damage initiation rule is based on the Rice-Tracey model:

$$DI = \frac{1}{\eta} \int_0^{\varepsilon_p} \exp(1.5T) \cdot d\varepsilon_p > 1 \quad (2-20)$$

A linear damage evolution rule is adopted (Figure 2.6), and the damage varies with the equivalent plastic displacement ( $u_{eq}$ ). The equivalent plastic displacement can be calculated as follows:

$$u_{eq} = L_e \cdot \varepsilon_p \quad (2-21)$$

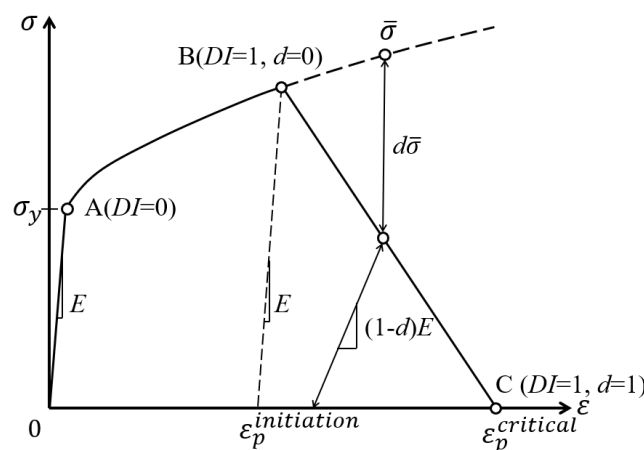


Figure 2.5 Three-stage and two-parameter ductile fracture model.



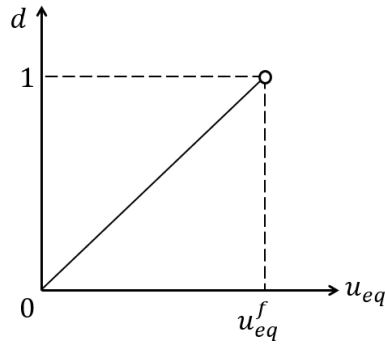


Figure 2.6 Damage evolution rule.

### 2.3.4 Method for crack propagation

#### 1. Element deletion technique

The delete element technique is born to overcome the defects of the finite element method, which is based on continuum mechanics. In the framework of continuum mechanics, the material bases are continuous in space, and the unit itself does not disappear. However, the material will break and separate under loadings, so the finite element software provides an element deletion function to simulate this situation. In ABAQUS, this technique can be achieved in two ways, i) define the embedded ductile fracture model and ii) call the user's subroutine VUMAT. The first way is restricted to several models, while the second one is more flexible since users can program their own methods.

#### 2. Cohesive zone method

In the cohesive zone method, the crystal in the material is combined by the cohesive forces, and a cohesion constitutive model is defined to describe the relationship between the cohesive force and relative displacement of two crack surfaces. In numerical modelling, cohesive elements are placed along the crack path; the crack can propagate inside the element without considering the mesh size. The cohesive element is widely used when the crack propagation path is known. In the 1960s, Dugdale [53] and

Barenblatt [54] introduced the concept of process zone. Their achievement in describing elastoplastic crack development is called the Dugdale-Barenblatt model, which is the predecessor of the cohesive model. Needleman [55] established a framework to describe the process of void nucleation from initial debonding through complete de-cohesion. Siegmund and Brocks [56] introduced stress triaxiality into the traction separation law and formed the triaxiality-dependent cohesive zone model.

As a phenomenological mechanics framework, the cohesive zone method has a broad scope of application. Still, its limitations are that it needs a more solid theory background, and its parameters have no clear physical meanings.

### 3. Extended finite element method (XFEM)

In 1999, Belytschko and Black [57] proposed a method to calculate the stress field of the crack tip, which is independent to the mesh generation. Subsequently, Moës and Belytschko [58] introduced two extended shape functions, the step function and crack-tip function, to describe the crack surface and crack tip, respectively, and named it the extended finite element method (XFEM). A physical crack (or discontinuity) needs to be introduced into the simulation, and the crack can propagate through the existing mesh. Sukumar et al. [59] improved XFEM and extended it to three-dimensional crack modelling. In the field of micro mechanism, Sukumar et al. [60] modelled holes and inclusions in the material by level sets in the XFEM.

The advantage of the extended finite element method is that it allows crack growth inside the element, so the mesh can be mapped without considering the crack shape. However, an initial crack is embedded in this method, which means XFEM fails to simulate the fracture initiation.

### **2.3.5 Application of ductile fracture models in high strength steels**

Several studies have investigated the applicability of ductile fracture models to high strength steels, including S690 and S960.

Yang et al. [61] calibrated the ductile damage model of high-strength steel S690Q, S700MC, and S960Q based on tensile coupon test results. The damage initiation criterion is based on the Rice-Tracey model. Yan et al. [38] studied the ductile failure of the cold-formed S700 material using the Gurson-Tvergaard-Needleman (GTN) damage model. The standard and notched coupon specimens with a circular hole in the centre were investigated. These two studies focus on the ductile fracture behaviour of high strength steels under high stress triaxiality state.

Wang et al. [62] conducted experimental investigations to study the influence of stress triaxiality and Lode angle on fracture ductility of three different high strength steels (Q550, Q690 and Q890). The experimental results are used to calibrate three typical fracture criteria: Void Growth Model, Combined Fracture Model and Bai-Wierzbicki Model. Ran et al. [63] investigated the influences of triaxial stress states on fracture ductility of weld metals, and a series of tests on specimens with moderate stress triaxiality and Lode parameters were carried out. By comparing three typical uncoupled fracture models, i.e. Void Growth Model, Maximum Shear Stress Model and Bai-Wierzbicki Model, Bai-Wierzbicki Model gives a precious prediction of ductile fracture of weld metals.

While several studies have provided promising results indicating that existing ductile fracture models may be reasonably accurate in modelling the failure of high strength steels under moderate to high stress triaxialities, the investigations of ductile fracture behaviour of base metal, weld metal and heat affected zone of high strength steel under shear and tension combined loading conditions are not sufficient. The complex

metallurgical microstructures and stress states introduced by the welding process can significantly impact fracture behaviours in high strength steels. Therefore, it is necessary to study welded sections to understand better how stress triaxiality and Lode parameters in the weld metal and heat-affected zones influence ductile fracture initiation and propagation.

## **2.4 Advanced measurement techniques in mechanical testing**

### **2.4.1 Digital image correlation (DIC)**

Digital image correlation (DIC) is a flexible and robust non-contact optical method utilized for quantifying displacement, strain, and deformation in various materials and at multiple length scales.

This process involves analyzing images of specimens subjected to various loading conditions. By employing correlation-based matching algorithms and numerical differentiation methods, quantitative information regarding displacement and strain responses can be extracted [64-67]. The capacity of DIC to deliver precise and comprehensive measurements of displacement and strain has established it as a critical tool across diverse disciplines, such as engineering, materials science, and biomechanics. Moreover, DIC offers a cost-effective and user-friendly alternative to many traditional experimental techniques, contributing to its widespread adoption in experimental mechanics.

The introduction of digital image correlation (DIC) techniques in mechanical testing has significantly improved the accuracy of test by providing high-precision, non-contact measurements of surface deformations. Initially developed by Sutton et al., DIC has evolved into an essential tool for investigating material responses under complex loading scenarios [68]. In particular, this technology has greatly facilitated the study of

post-necking behavior in steel, enabling the acquisition of full-field strain distributions with exceptional spatial resolution. Furthermore, Pan et al. conducted a comprehensive review of DIC methodologies, emphasizing their effectiveness in capturing heterogeneous deformations that occur after necking in metal specimens subjected to tensile testing [69].

#### **2.4.2 X-ray computed tomography (CT)**

X-ray computed tomography is an advanced scanning and imaging method in which a narrow beam of X-ray is emitted to a specimen which rotates intermittently for scanning. Signals are received and then processed to generate cross-sectional images. These images are digitally “stacked” together to form a three-dimensional (3D) image of the specimen that allows for identification of internal defects within the specimen. The application of X-ray CT in materials science began in the late 20th century, primarily for non-destructive evaluation. Early studies, such as those by Stock [70], demonstrated potentials of X-ray microtomography to reveal internal features of materials without causing any damage. These initial applications were the basis for detailed investigations into the microstructural aspects of fracture. With improvements in resolutions and scanning speeds of X-ray CT technology, its application was expanded to include the study of micro-voids in metals, and the high-resolution imaging was able to capture intricate details of void morphology and distributions [71].

Recent studies have exploited X-ray CT to examine micro-void evolutions in structural steel under loadings. Buffière et al. [72] utilized X-ray tomography to observe void growth in metals under tensile actions, providing valuable data on locations and growth rates of void nucleation. Toda et al. [73] have used X-ray CT to investigate initiation and growth of voids in aluminium alloys, providing evidence applicable to steel fracture

mechanics. Similarly, Zhang et al. [74] employed in-situ X-ray CT to investigate typical fracture process in mild steel, providing a detailed analysis on void nucleation, growth and coalescence. The application of X-ray CT to structural steel has provided valuable insights into the micro-mechanisms of ductile fracture in these steel.

## Chapter 3

### Determination of true stress-strain curves of S355-S960 steels through modified Bridgman method

#### 3.1. Introduction

Owing to advances in steel-making technology over the past two decades, high strength steel with yield strengths  $f_y$  at 690 and 960 N/mm<sup>2</sup> are readily available in many parts of the world. Many structural engineers are eager to take advantage of these high strength steel for construction of structural members in multi-storey buildings and long span bridges because of their enhanced strengths. However, there is a concern about their ductility which is quantified as the elongation limit  $\varepsilon_L$  at fracture. Hence, there is a need to examine the deformation characteristics of these high strength steel, in particular, their engineering stress-strain ( $\sigma_e - \varepsilon_e$ ) curves, and also their true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves for engineering applications.

An experimental and numerical investigation into the true stress-strain characteristics of S355 to S960 structural steels was conducted and presented in this paper. The investigation includes the following:

- a) Standard tensile tests on cylindrical coupons of S355, S460, S690 and S960 steel were carried out. Advanced digital imaging technique was employed to capture instantaneous dimensions of these coupons using a high-resolution digital camera throughout testing.
- b) Advanced digital processing technique was employed to detect both the cross-sectional radius  $a_i$  and the in-plane longitudinal radius  $R_i$  of the necking regions of

the coupons. Based on various established edge detection algorithms available in the literature, the values of both  $a_i$  and  $R_i$  for all the coupons of S355, S460, S690 and S960 steel were obtained. Hence, an expression relating to the spatial necking ratio  $a/R$  to average strains  $\varepsilon_{av}$  of the coupons of various steel is readily obtained, and the corresponding true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves are also derived.

- c) In order to verify the accuracy of the true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves obtained from Item b) above, a finite element model is established, and the true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves were adopted to simulate the standard tensile tests. Comparison between predicted and measured applied load – extension ( $N - \Delta$ ) curves of tensile tests on various steels is made.
- d) Comparisons with the predicted results obtained from the Modified Bridgman Method proposed above with those of *iAreaM* are also made, and numerical accuracy and computational efficiency of both methods are discussed in a thorough manner.

### 3.2. Brief review on correction methods

For ease of discussion, a brief review on various methods available in the literature is presented in this section.

#### 3.2.1. Bridgman Method

It is well known that the Bridgman Method is based on three assumptions: i)  $\varepsilon_{rr} = \varepsilon_{\theta\theta}$  for all values of  $r$  [1]; ii) the relationship between the principal stresses at the surface of the minimum cross section follows the Tresca yield criterion; iii) the contour of the necking region is approximated by a circle. As shown in [Figure 3.1](#), the necking region of a coupon is illustrated in polar coordinates, and  $\varepsilon_{rr}$  and  $\varepsilon_{\theta\theta}$  are the radial and the circumferential strains, respectively. From assumption i), it is shown that when  $z = 0$ ,



$\sigma_{rr} = \sigma_{\theta\theta}$ . Based on this, when the minimum section of the coupon starts to yield, the von Mises yield criterion may be re-presented with the Tresca yield criterion for the points on the surface, and hence, the second assumption follows.

The von Mises yield criterion is expressed in Eq. 3-1 while assumption ii) is expressed in Eq. 3-2, as follows:

$$\bar{\sigma} = \sqrt{\frac{1}{2}[(\sigma_{rr} - \sigma_{\theta\theta})^2 + (\sigma_{\theta\theta} - \sigma_{zz})^2 + (\sigma_{zz} - \sigma_{rr})^2]} \quad (3-1)$$

$$\bar{\sigma} = \sigma_{zz} - \sigma_{rr} = \sigma_{zz} - \sigma_{\theta\theta} = f \quad (3-2)$$

where  $f = \sigma_{zz}|_{x=a}$  denotes the stress in  $z$  direction of the surface point, which is readily calculated to give the axial force  $N$ :

$$N = \int_0^a 2 \pi r \sigma_{zz} dr \quad (3-3)$$

Based on assumptions (i) and (ii), the equilibrium equation is given by:

$$\frac{d \sigma_{rr}}{dr} + \frac{f}{R_\sigma} = 0 \quad (3-4)$$

where  $1/R_\sigma$  is curvature of the principal stress surface. Important stress components mentioned above are illustrated in Figure 3.3. According to assumption iii), the following expression follows:

$$R_\sigma = \frac{1}{2} \left[ \frac{a(a + 2R)}{r} - r \right] \quad (3-5)$$

where  $a$  is the radius of the minimum cross section,  $R$  is the radius of the necking region.

Substituting Eq. 3-5 into Eq. 3-4 gives:

$$\sigma_{rr} = \sigma_{\theta\theta} = f \cdot \ln \left( \frac{a(a + 2R) - r^2}{2 a R} \right) \quad (3-6)$$

$$\sigma_{zz} = f \cdot \left[ 1 + \ln \left( \frac{a(a + 2R) - r^2}{2 a R} \right) \right] \quad (3-7)$$

When  $r = 0$ , the true stress is given by Eq. 3-8 according to Eq. 3-3 and Eq. 3-7:

$$\sigma_t = \bar{\sigma} = \frac{1}{\left(1 + \frac{2R}{a}\right) \cdot \ln \left(1 + \frac{a}{2R}\right)} \cdot \frac{N}{\pi a^2} \quad (3-8)$$

Re-writing

$$\sigma_t = \eta_{\sigma}^B \cdot \sigma_{av} \quad (3-9)$$

where

$\sigma_{av}$  is the average stress, i.e.  $\frac{N}{\pi a^2}$

$\eta_{\sigma}^B$  is the stress modification factor

$$= \frac{1}{\left(1 + \frac{2R}{a}\right) \cdot \ln \left(1 + \frac{a}{2R}\right)} \quad (3-10)$$

In order to facilitate application of the method, Bridgman [1] proposed an empirical expression between the  $a/R$  ratio and the radius  $a$ . Hence, the  $a/R$  ratio and also the  $\eta_{\sigma}^B$  factor are readily obtained with measured values of  $a$ . Based on results of a number of tensile tests on various metals, the  $a/R$  ratio was found to be directly dependent on the average strain  $\varepsilon_{av}$  as follows:

$$\frac{a}{R} = c_{\omega} \cdot (\varepsilon_{av} - \varepsilon_n) \quad (3-11)$$

where

$c_\omega$  is a constant, and its value is readily obtained from tests, and

$\varepsilon_n$  is the average strain at the point of onset of necking, and its value is also obtained from tests.

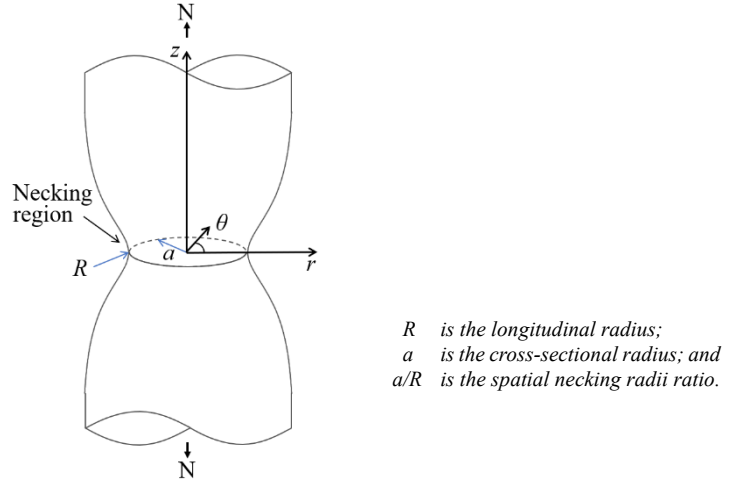


Figure 3.1 Necking in a cylindrical coupon.

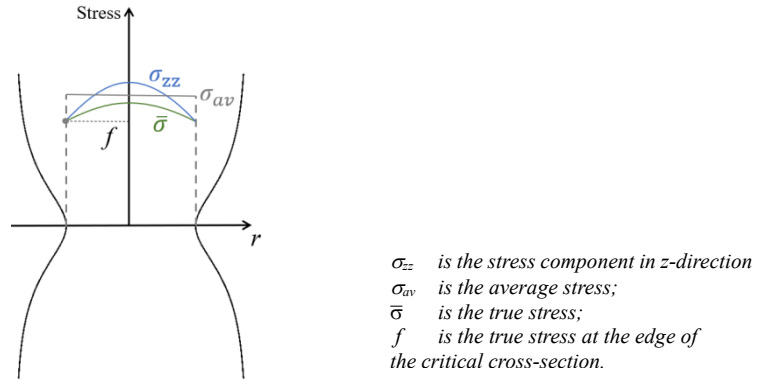
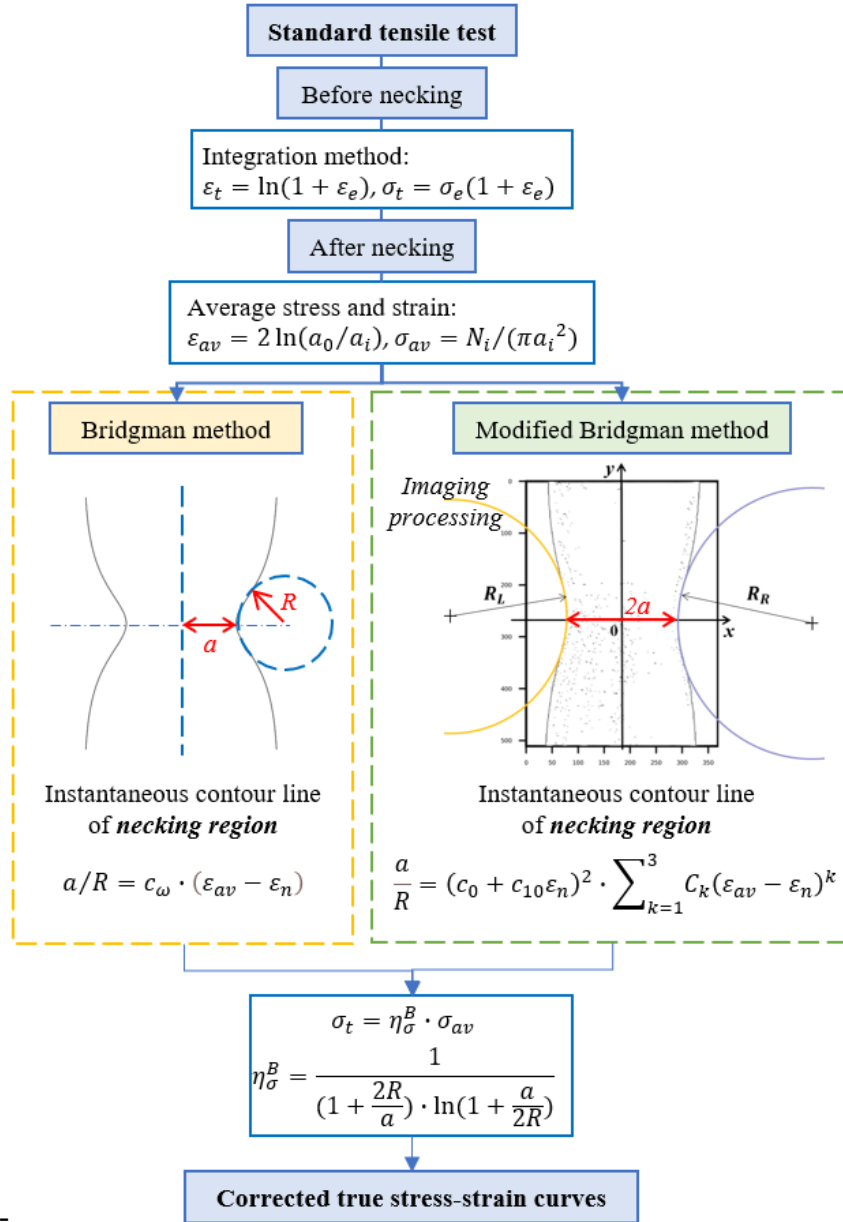
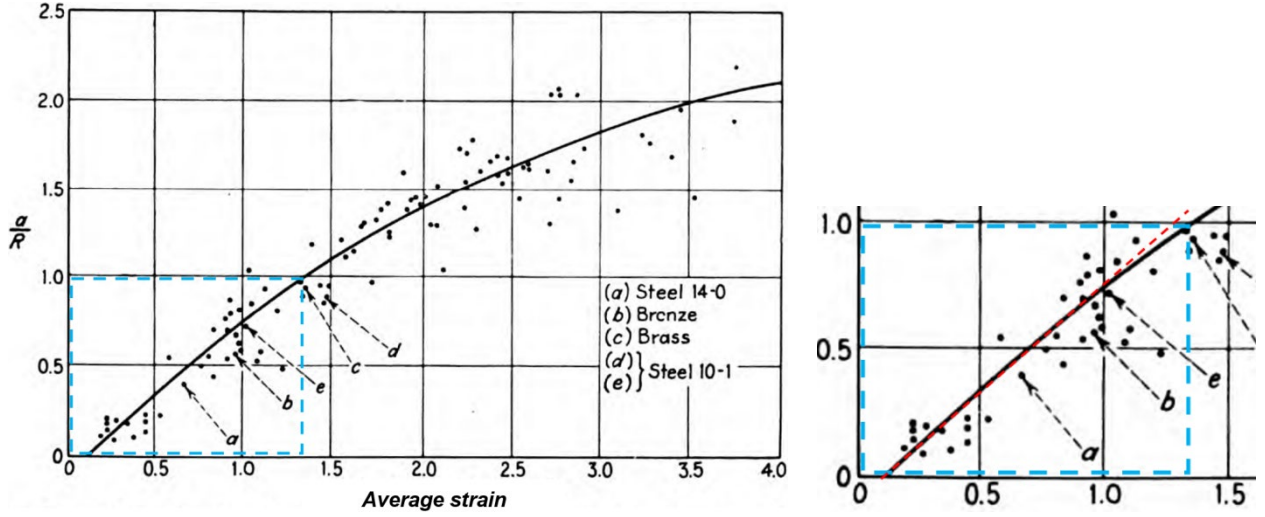


Figure 3.2 Stress distribution across the critical cross-section.

Figure 3.3 illustrates the procedure of the Bridgman method, and two sets of measured data are needed, namely, the instantaneous cross-sectional radius  $a$ , and the in-plane curvature  $1/R$  of the necking region of the coupon. Bridgman compared the test data with the proposed expression (Eq. 11) in Figure 3.4, and apparent scattering in the test data is apparent.



**Figure 3.3** Extension on Bridgman's Method.



**Figure 3.4** Variations of  $a/R$  against average strain  $\varepsilon_{av} = 2 \ln (a_0/a)$  given by Bridgman [1].

### 3.2.2. Instantaneous Area Method

The Instantaneous Area Method (*iAreaM*) [14] is primarily a numerical method which determines both the true stress  $\sigma_t$  and the true strain  $\varepsilon_t$  of the minimum cross-section of a coupon after necking as follows:

$$\varepsilon_t = \varepsilon_{av} \cdot \eta_\varepsilon^I \quad (3-12)$$

$$\sigma_t = \sigma_{av} \cdot \eta_\sigma^I \quad (3-13)$$

Both the correction factors  $\eta_\varepsilon^I$  and  $\eta_\sigma^I$  are obtained after systematic data analyses on a finite element model, and they are given by:

$$\eta_\varepsilon^I = \frac{\varepsilon_{p,max}}{\varepsilon_{av}} \quad (3-14)$$

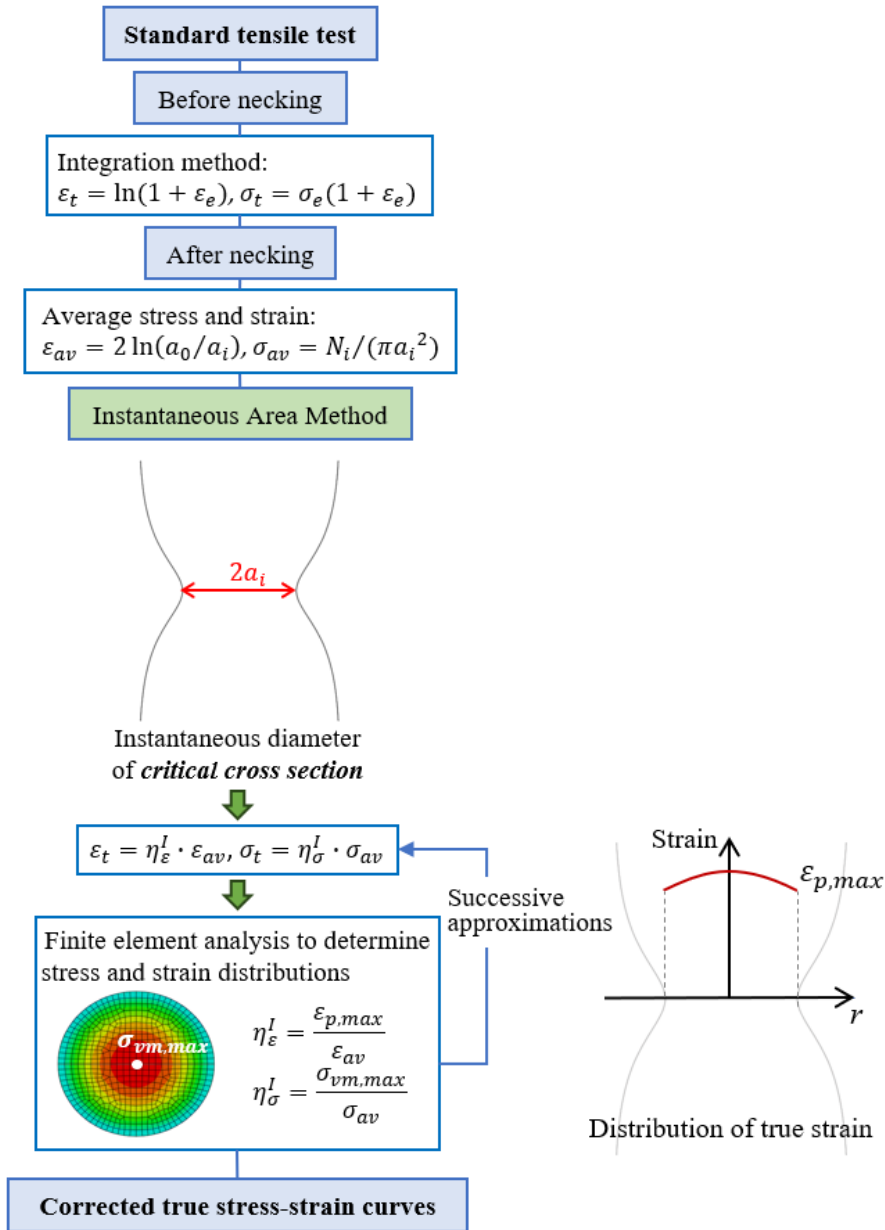
$$\eta_\sigma^I = \frac{\sigma_{vm,max}}{\sigma_{av}} \quad (3-15)$$

where

$\varepsilon_{p,max}$  and  $\sigma_{vm,max}$  are the maximum values of the plastic strain and the von Mises stress of the minimum cross-section respectively.

A number of successive approximations are often necessary to establish convergence

of these two correction factors. After evaluating  $\eta_{\varepsilon}^I$  and  $\eta_{\sigma}^I$  according to Eq. 3-14 and Eq. 3-15, the values of  $\sigma_t$  and  $\varepsilon_t$  are then updated according to Eq. 12 and Eq. 3-13. These values are then adopted as the updated stress-strain curve of the steel for a re-run of the finite element model. Hence, an improved applied load-extension ( $N - \Delta$ ) curve as well as the corresponding distributions of stresses and strains at the minimum cross-section of the coupon are then obtained. By repeating the process until the convergence of these correction factors  $\eta_{\varepsilon}^I$  and  $\eta_{\sigma}^I$  is achieved, the updated true stress-strain ( $\sigma_t - \varepsilon_t$ ) curve of the coupon is hence established. Figure 3.5 illustrates the procedure of *iAreaM*, and only one set of test data, i.e. the instantaneous diameter  $a$  of the minimum cross section of the necking region of the coupon is required.



**Figure 3.5** Illustration of Instantaneous Area Method.

### 3.3. Experimental investigations

A total of 16 tensile tests on standard cylindrical coupons were conducted to provide data for development on both engineering and true stress-strain ( $\sigma_t$  -  $\varepsilon_t$ ) curves of various steel grades.

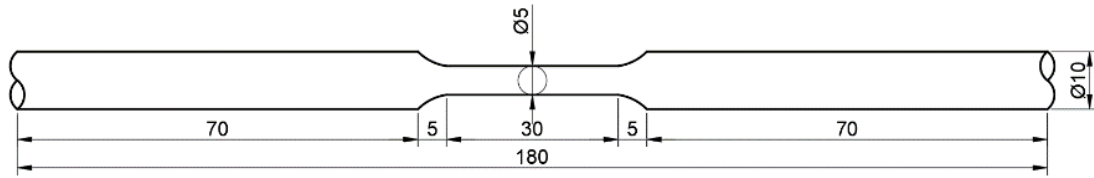
#### 3.3.1. Tensile tests on standard cylindrical coupons

**Table 3-1** presents the chemical composition of the four steel, all of them are shown to be in full compliance with the requirements given in EN 10025-2 and 10025-6. Typical dimensions of a standard cylindrical coupon are illustrated in **Figure 3.6**. The gauge length and the nominal diameter of the parallel region of the coupon is 25.0 and 5.0 mm respectively, and these meet the requirements of BS EN ISO 6892-1:2019 [75]. All the monotonic tensile tests were carried out using a precision tensile test machine, as shown in **Figure 3.7**, and both an extensometer and a high-resolution digital camera were employed to capture deformations of the coupons during tests. Digital photos were taken every 30 seconds to capture images of the coupons for subsequent data analysis. The test set-up is illustrated in **Figure 3.7**. All the tests were conducted in a displacement control. The testing rate before yielding was 0.05 mm/min, and it was gradually increased to 0.10 mm/min after yielding and before necking. After that, it was maintained to be 0.20 mm/min until fracture.

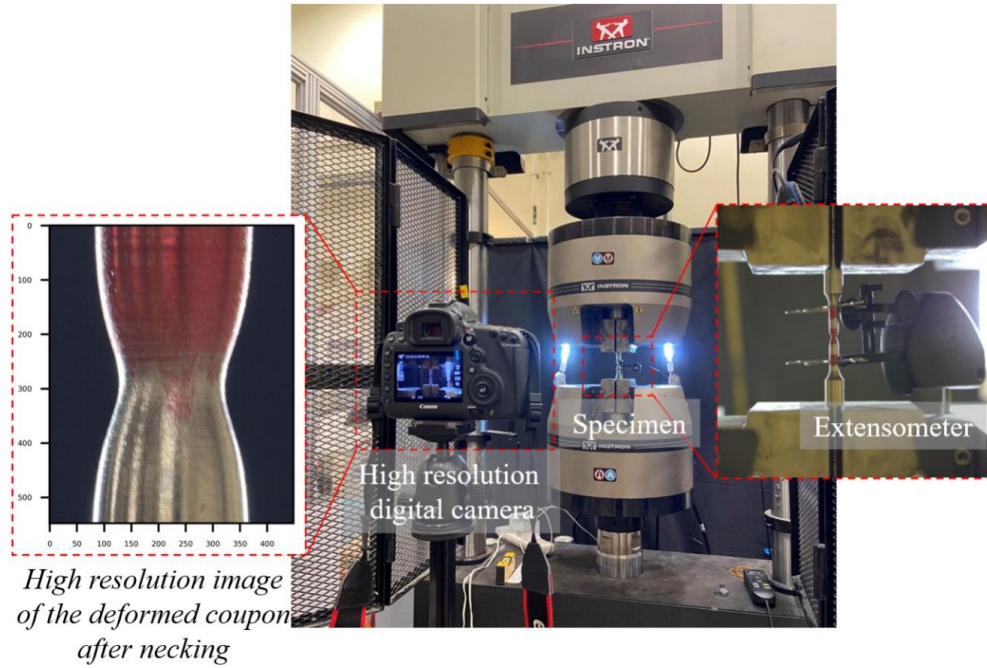
**Table 3-1** Chemical compositions.

| Steel Grade | C    | Si   | Mn   | P     | S     | N     | B     | Cr   | Cu   | Mo    | Nb    | Ni    | Ti    | V     | Zr   |
|-------------|------|------|------|-------|-------|-------|-------|------|------|-------|-------|-------|-------|-------|------|
| S355        | 0.23 | 0.60 | 1.70 | 0.035 | 0.035 | 0.014 | -     | -    | 0.60 | -     | -     | -     | -     | -     | -    |
| S460        | 0.17 | 0.48 | 1.50 | 0.012 | 0.005 | 0.005 | -     | 0.02 | 0.03 | 0.002 | 0.046 | 0.018 | 0.002 | 0.087 | -    |
| S690        | 0.22 | 0.86 | 1.80 | 0.025 | 0.012 | 0.016 | 0.006 | 1.60 | 0.55 | 0.74  | 0.07  | 2.10  | 0.07  | 0.14  | 0.17 |
| S960        | 0.17 | 0.25 | 1.10 | 0.010 | 0.002 | 0.015 | 0.005 | 1.50 | 0.50 | 0.70  | 0.06  | -     | 0.05  | -     | -    |





**Figure 3.6** Geometry and dimensions of a standard coupon.

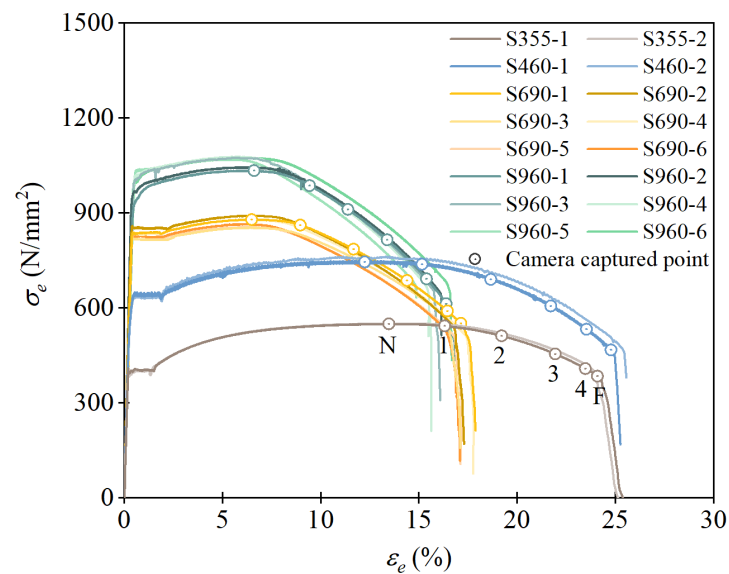


**Figure 3.7** Test setup for monotonic tensile tests.

### 3.3.2. Typical results

The measured engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curves for the four steels are plotted in **Figure 3.8**, and their key mechanical properties are summarized in **Table 3-2**. High resolution images of the necking regions of the coupons at different deformation stages are shown in **Table 3-3** while these deformation stages of these coupons are marked in **Figure 3.8** for easy reference. For ease of presentation, Coupon S355-1 is taken as an example, and there is a total of 6 deformation stages to be considered. Firstly, the onset of necking in the coupon is marked as Point N. As the extension increases progressively, Points 1 to 4 are marked so that the post-necking deformation range of the true stress-strain curve is roughly divided into equal intervals after necking, i.e. Points 1, 2, 3 and

4. Towards the end of the test, Point F is identified just before fracture, i.e. at which a sudden change in the slope of the curve takes place. It should be noted that since the focal length of the digital camera remains unchanged, all dimensions of the deformed coupon correspond directly to the number of pixels captured in the digital images. Therefore, the instantaneous dimensions of the necking region of the coupon are readily obtained through counting of the pixels in these images.

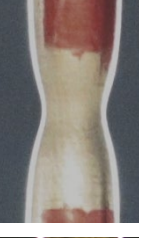
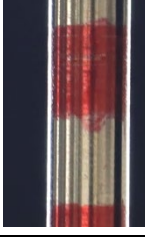
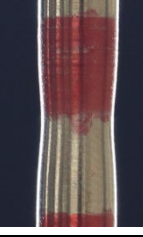


**Figure 3.8** Engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curves of S355 to S960 steel.

**Table 3-2** Measured mechanical properties of S355 to S960 steels

| Coupons | Young's modulus<br>$E$<br>(kN/mm <sup>2</sup> ) | Yield strength<br>$f_y$<br>(N/mm <sup>2</sup> ) | Tensile strength<br>$f_u$<br>(N/mm <sup>2</sup> ) | Strain at $f_u$<br>$\epsilon_u$<br>(%) | Strain at fracture<br>$\epsilon_L$<br>(%) |
|---------|-------------------------------------------------|-------------------------------------------------|---------------------------------------------------|----------------------------------------|-------------------------------------------|
| S355-1  | 209                                             | 395                                             | 550                                               | 13.5                                   | 24.3                                      |
| S355-2  | 210                                             | 388                                             | 551                                               | 13.9                                   | 24.2                                      |
| S460-1  | 215                                             | 627                                             | 751                                               | 12.2                                   | 25.0                                      |
| S460-2  | 214                                             | 621                                             | 753                                               | 13.4                                   | 26.1                                      |
| S690-1  | 224                                             | 833                                             | 879                                               | 8.3                                    | 17.9                                      |
| S690-2  | 219                                             | 850                                             | 892                                               | 6.5                                    | 17.3                                      |
| S690-3  | 213                                             | 826                                             | 878                                               | 6.6                                    | 17.8                                      |
| S690-4  | 212                                             | 814                                             | 868                                               | 7.3                                    | 17.1                                      |
| S690-5  | 221                                             | 823                                             | 864                                               | 6.1                                    | 17.1                                      |
| S690-6  | 215                                             | 818                                             | 855                                               | 6.4                                    | 17.2                                      |
| S960-1  | 206                                             | 944                                             | 1,034                                             | 6.6                                    | 16.4                                      |
| S960-2  | 211                                             | 973                                             | 1,045                                             | 6.4                                    | 16.2                                      |
| S960-3  | 205                                             | 1,009                                           | 1,076                                             | 5.8                                    | 16.1                                      |
| S960-4  | 202                                             | 1,018                                           | 1,078                                             | 5.7                                    | 15.6                                      |
| S960-5  | 214                                             | 1,032                                           | 1,069                                             | 5.5                                    | 14.9                                      |
| S960-6  | 204                                             | 998                                             | 1,053                                             | 6.2                                    | 16.7                                      |

**Table 3-3** High resolution images of necking regions at different deformation stages

| Coupons | Point N                                                                             | Point 1                                                                             | Point 2                                                                             | Point 3                                                                              | Point 4                                                                               | Point F                                                                               |
|---------|-------------------------------------------------------------------------------------|-------------------------------------------------------------------------------------|-------------------------------------------------------------------------------------|--------------------------------------------------------------------------------------|---------------------------------------------------------------------------------------|---------------------------------------------------------------------------------------|
| S355-1  |    |    |    |    |    |    |
| S460-1  |    |    |    |    |    |    |
| S690-1  |   |   |   |   |   |   |
| S960-1  |  |  |  |  |  |  |

The flow chart on how to determine instantaneous dimensions after necking is illustrated in [Figure 3.9](#). It should be noted that there is a high level of consistency among all the test results of the same steel grades, and hence, only the test results of a representative coupon for each steel grade are presented. [Figure 3.10](#) plots the variations of the instantaneous diameters  $a_i$  of the necking regions of the coupons of the four steel grades during tests, and it is evident that the values of  $a_i$  of all these steels are decreased in a non-linear manner as  $\varepsilon_e$  increase.

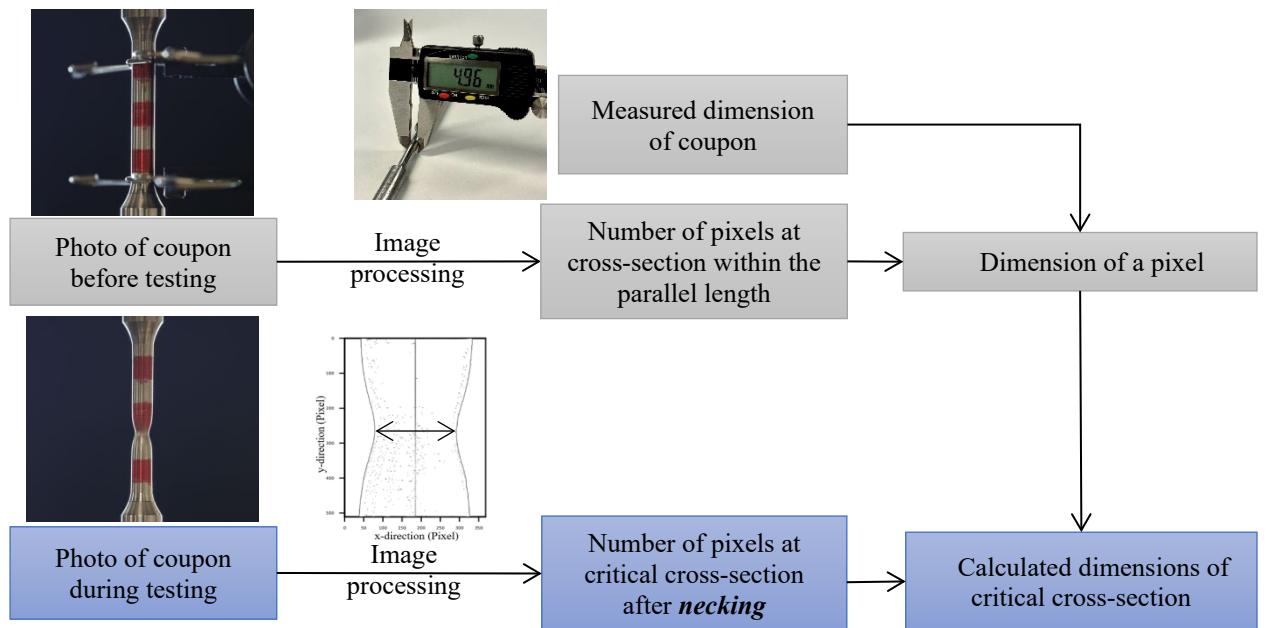


Figure 3.9 Flow chart on how to determine instantaneous dimensions after necking.

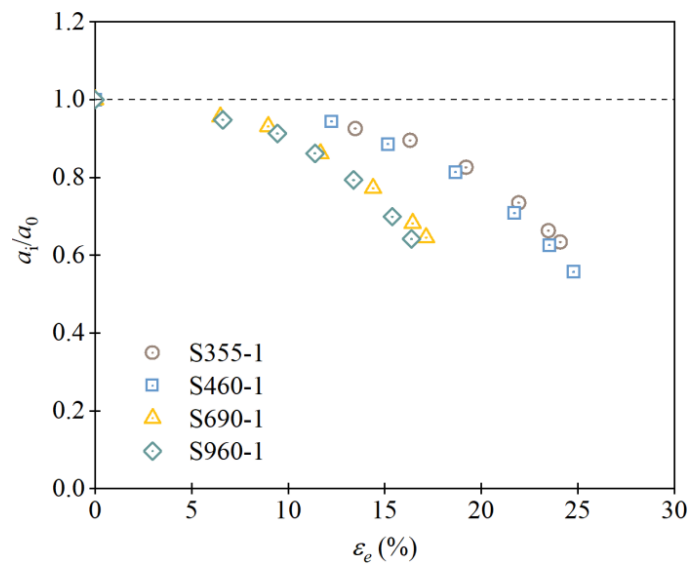


Figure 3.10 Variation of instantaneous radius against engineering strain  $\epsilon_e$ .

### 3.3.3. Measurement of the in-plane radius $R$ of the necking region

In order to identify and locate numerically both the left and the right edges of the necking region of the coupon, high resolution digital images of the deformed coupon are taken at a close time interval under specific lighting conditions to enhance brightness, and hence sharpness, of the deformed coupon, as shown in Figure 3.11 (a). An image processing software is developed to convert the digital images into black and white so as to emphasize the edges of the necking region, and a typical result processed by software is illustrated in Figure 3.11 (b). By developing specific rules on both the colour and the brightness of the pixels for statistical analyses to ensure a precise edge detection, both the left and the right edges of the deformed coupon, and also their coordinates, are readily identified. After plotting these edges of the deformed coupons in the form of dot-matrix, as shown in Figure 3.11 (c), it is overlaid with the image of the original image of the deformed coupon with red lines for verification (Figure 3.11 (d)).

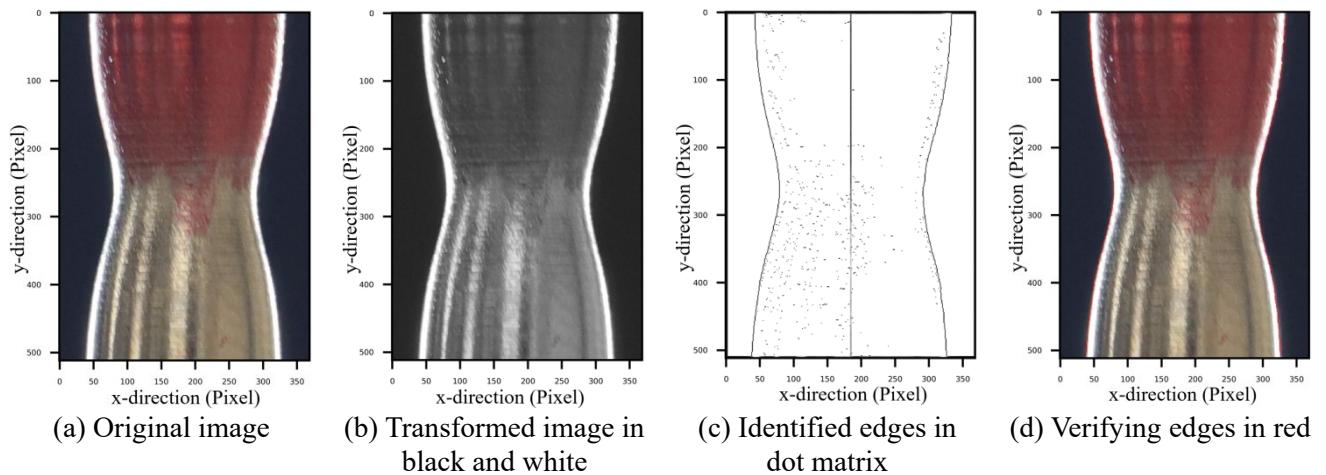
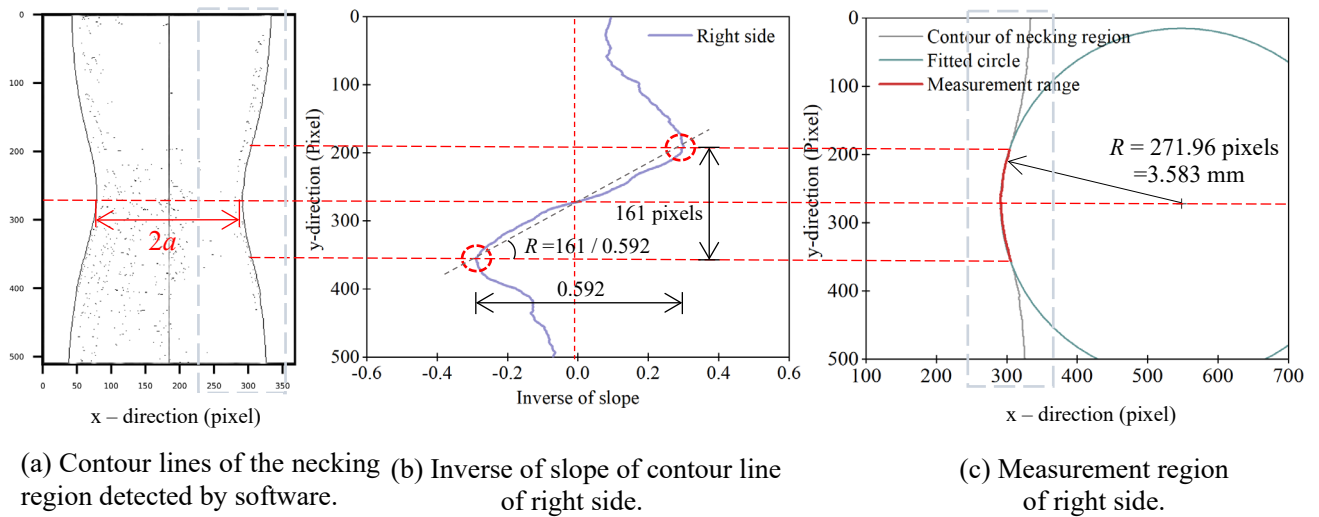


Figure 3.11 Typical digital results of image processing. Point 4 with  $\epsilon_{av} = 0.8$

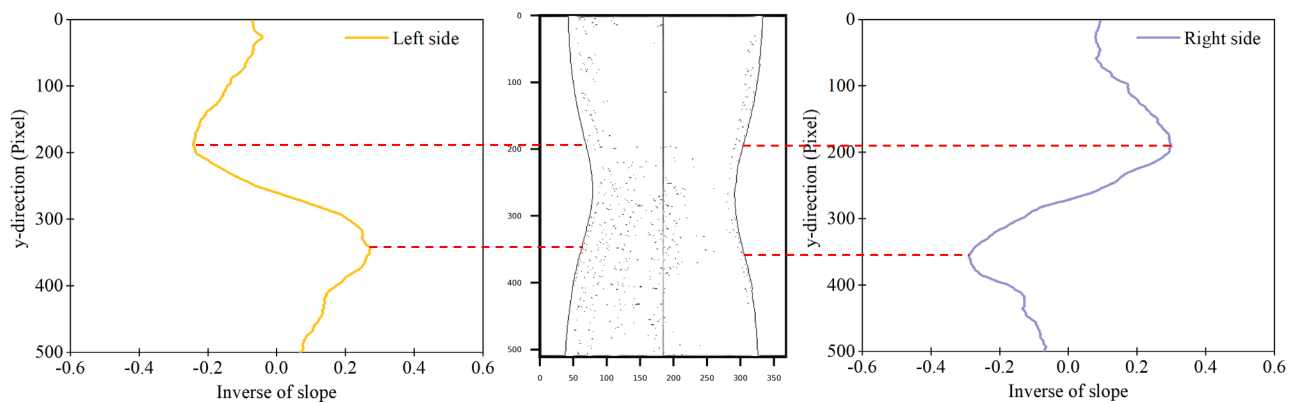
Once the coordinates of the edges of the necking region of the deformed coupon are established (for example, Figure 3.12 (a)), the slope along the right edge of the necking

region is readily evaluated numerically at each pixel. The values of inverses of slopes along the right edge of the deformed coupon are plotted in Figure 3.12 (b). It is noted that there are two points of contra-flexure along the curve of the inverse of slope, and the region between these two points is determined as the measurement range for  $R$ , as highlighted in Figure 3.12 (c). The change of the inverse of slope within the measurement range is the radius of curvature  $R$  of one side. Figure 3.13 illustrates a typical plot of the numerical values of the slopes along both the left and the right edges of the necking region, and hence, both the in-plane radii  $R_L$  and  $R_R$  of the necking region are obtained as shown in Figure 3.14.

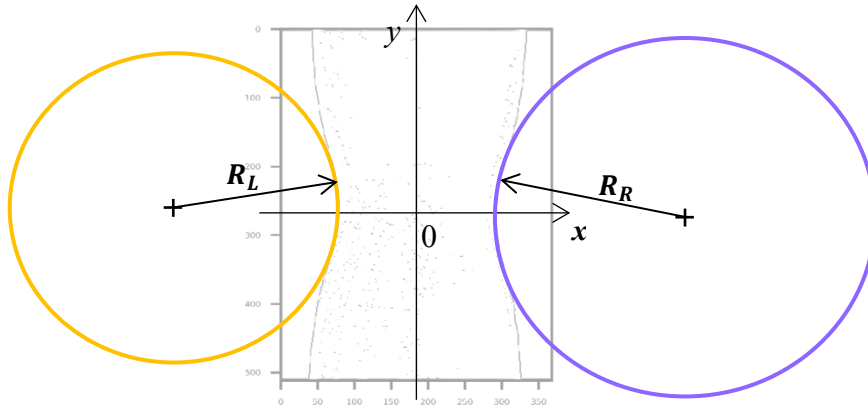


**Figure 3.12** Digital photo analysis for edge detection and determination of  $a$  and  $R$   
- Point 4 with  $\epsilon_{av} = 0.7$ .

Note: Original width = 4.98 mm  $\equiv$  378 pixels



**Figure 3.13** The inverse of the slope along the y-direction - Point 4 with  $\epsilon_{av} = 0.7$ .



**Figure 3.14** Illustration of determination of  $R$  at two sides.

Hence, the measured instantaneous cross-sectional radius  $a$  and the measured in-plane longitudinal radii  $R_L$  and  $R_R$  of the necking regions of various coupons are summarized in **Table 3-4**. It should be noted that the measured values of  $R_L$  and  $R_R$  are different from one another at various deformation stages by about 5 to 20%, and hence, the deformed shapes of the coupons are not symmetrical along both the longitudinal and the transverse axes of the coupons. This observation is inconsistent with the general assumption of axisymmetric necking commonly adopted in many correction methods. This is probably a result of non-uniformity within the coupons, such as material and geometrical anisotropy, and mis-alignment during installation and load application. In general, the average values of  $R$  are adopted in subsequent analyses to minimize the effects of such a non-uniformity. Based on a number of sensitivity studies on the values of  $R$  at typical deformations after necking with a variation up to 20%, the values of the correction factors are found to vary merely within 2%.



**Table 3-4** Values of  $a$  and  $R$  of various coupons obtained from image processing.

| Deformation stages | $a$<br>(mm) | $R_L$<br>(mm) | $R_R$<br>(mm) | $R = \frac{R_L + R_R}{2}$<br>(mm) | $a/R$ |
|--------------------|-------------|---------------|---------------|-----------------------------------|-------|
| Test S355-1        |             |               |               |                                   |       |
| Point N            | 2.300       | -             | -             | -                                 | 0.000 |
| Point 1            | 2.210       | 48.628        | 46.628        | 47.628                            | 0.046 |
| Point 2            | 2.023       | 15.270        | 17.241        | 16.256                            | 0.124 |
| Point 3            | 1.806       | 6.636         | 7.953         | 7.295                             | 0.248 |
| Point 4            | 1.633       | 3.808         | 3.543         | 3.676                             | 0.444 |
| Point F            | 1.560       | 2.674         | 3.255         | 2.964                             | 0.526 |
| Test S460-1        |             |               |               |                                   |       |
| Point N            | 2.332       | -             | -             | -                                 | 0.000 |
| Point 1            | 2.189       | 48.560        | 45.720        | 47.140                            | 0.046 |
| Point 2            | 2.020       | 15.456        | 17.394        | 16.425                            | 0.123 |
| Point 3            | 1.750       | 5.897         | 5.683         | 5.790                             | 0.302 |
| Point 4            | 1.531       | 2.587         | 2.815         | 2.701                             | 0.567 |
| Point F            | 1.366       | 2.008         | 1.727         | 1.867                             | 0.732 |
| Test S690-1        |             |               |               |                                   |       |
| Point N            | 2.360       | -             | -             | -                                 | 0.000 |
| Point 1            | 2.300       | 91.709        | 106.731       | 99.220                            | 0.023 |
| Point 2            | 2.127       | 21.593        | 15.370        | 18.481                            | 0.115 |
| Point 3            | 1.900       | 6.237         | 7.782         | 7.009                             | 0.271 |
| Point 4            | 1.682       | 3.876         | 3.090         | 3.483                             | 0.483 |
| Point F            | 1.590       | 2.659         | 2.781         | 2.720                             | 0.584 |
| Test S960-1        |             |               |               |                                   |       |
| Point N            | 2.360       | -             | -             | -                                 | 0.000 |
| Point 1            | 2.274       | 35.360        | 45.260        | 40.310                            | 0.056 |
| Point 2            | 2.146       | 18.032        | 16.575        | 17.304                            | 0.124 |
| Point 3            | 1.976       | 7.160         | 8.940         | 8.050                             | 0.245 |
| Point 4            | 1.741       | 3.539         | 3.583         | 3.561                             | 0.489 |
| Point F            | 1.598       | 2.480         | 2.520         | 2.500                             | 0.639 |

### 3.3.4. A modified empirical expression for prediction of $a/R$

With availability of all the measured data on both  $a$  and  $R$  of the coupons of various steel, it is highly beneficial to propose a modified empirical expression to determine the values of  $a/R$ , which is defined as the spatial necking radii ratio of the necking region at various average strains  $\varepsilon_{av}$ . Based on the measured data described in [Chapter 3.3.3](#) and summarized in [Table 3-4](#), it is demonstrated that the contour lines of the necking regions of the deformed coupons are related to the steel grades of the coupons. It is well known [76, 77] that in a standard tensile test, the yield strength has little influence on the stress evolution of the deformed coupon after necking, and instead, it is the hardening exponent ( $n$ ) that controls the hardening behavior. Numerically, the value of



the hardening exponent is equal to the average strain  $\varepsilon_{av}$  of the deformed coupon at necking, i.e.  $\varepsilon_{av} = \varepsilon_n$ . The relationships between the spatial necking radii ratio  $a/R$  and the average strain  $\varepsilon_{av}$  for these coupons of four steel grades are illustrated in [Figure 3.15](#). It should be noted that at the onset of necking,  $R$  is close to a positive infinity, and hence, the  $a/R$  ratio is close to zero. As the axial deformation increases, necking is further developed, and the  $a/R$  ratio may be readily described as follows:

$$\frac{a}{R} = (c_0 + c_{10}\varepsilon_n)^2 \cdot \sum_{k=1}^3 C_k (\varepsilon_{av} - \varepsilon_n)^k \quad (3-16)$$

where  $C_k$ ,  $c_0$  and  $c_{10}$  are constant to be evaluated. By fitting all the points in [Figure 3.15](#), the values of the constants are found to be as follows:

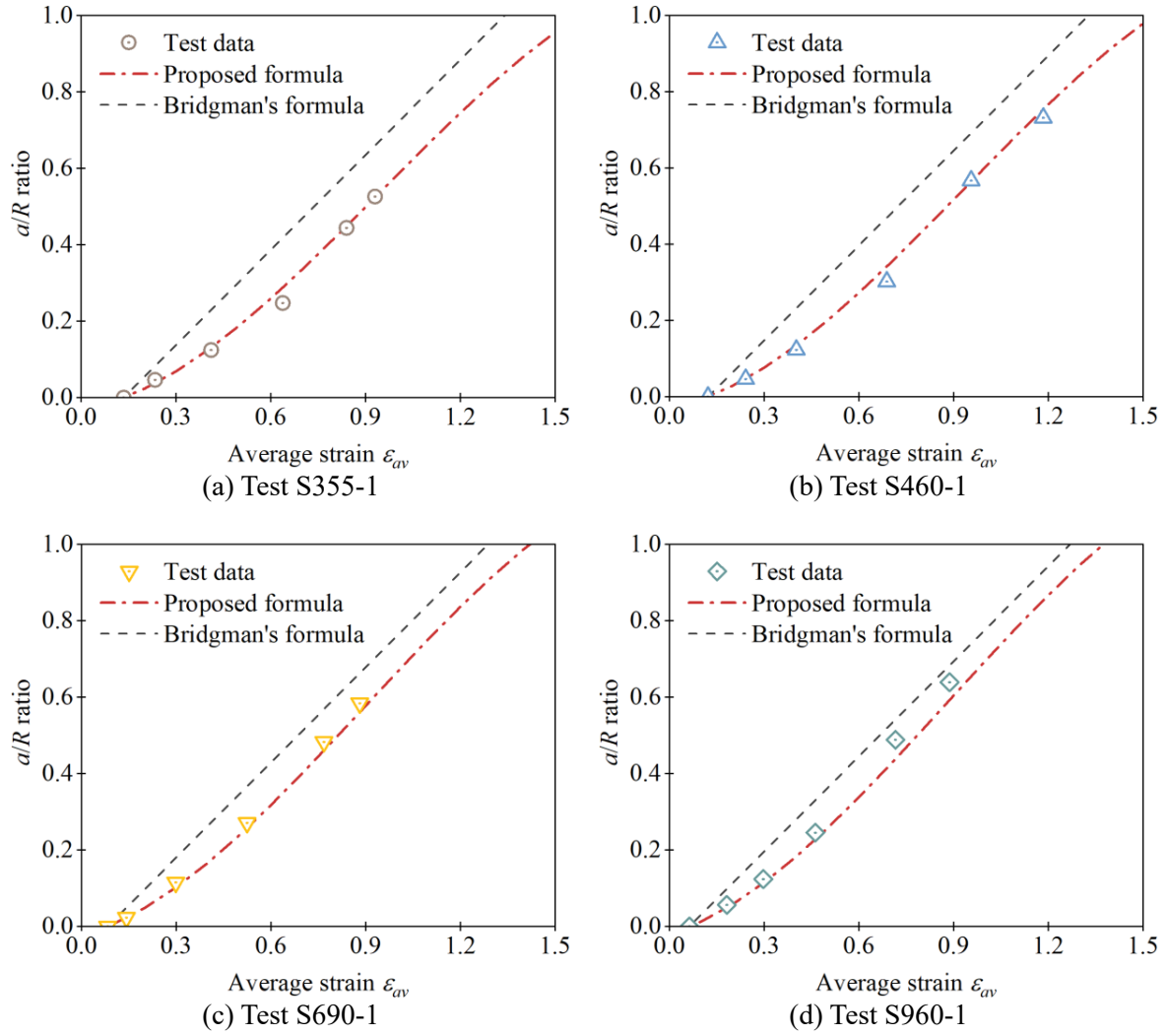
$$c_0 = 1.139, \text{ and } c_{10} = -1.219. \quad (3-17)$$

$$C_1 = 0.325, C_2 = 0.654, C_3 = -0.268. \quad (3-18)$$

The values of  $\varepsilon_n$  for various steel are summarized in [Table 3-5](#). Hence, the values of  $a/R$  are readily evaluated, and they are directly adopted in [Eq. \(13\)](#) to determine the values of the stress correction factor  $\eta_{\sigma}^B$  at various average strains to predict the corrected true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves. [Figure 3.16](#) plots all the corrected true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves of S355 to S960 steels predicted through the use of the proposed modified Bridgman method based on the proposed empirical expression, i.e. [Eq. \(25\)](#). It is shown that the proposed method always gives a true stress-strain ( $\sigma_t - \varepsilon_t$ ) curve which is below that of the average stress-strain ( $\sigma_{av} - \varepsilon_{av}$ ) curve, but slightly above the Bridgman method.

**Table 3-5** Calibrated parameters

| Steel | $\varepsilon_n$ | Bridgman method | Modified Bridgman method |          |       |       |        |
|-------|-----------------|-----------------|--------------------------|----------|-------|-------|--------|
|       |                 | $c_0$           | $c_0$                    | $c_{10}$ | $C_1$ | $C_2$ | $C_3$  |
| S355  | 0.1347          | 0.83            | 1.139                    | -1.219   | 0.325 | 0.654 | -0.268 |
| S460  | 0.1225          |                 |                          |          |       |       |        |
| S690  | 0.0830          |                 |                          |          |       |       |        |
| S960  | 0.0641          |                 |                          |          |       |       |        |



**Figure 3.15** Measured  $a/R$  ratio versus proposed  $a/R$  curves.

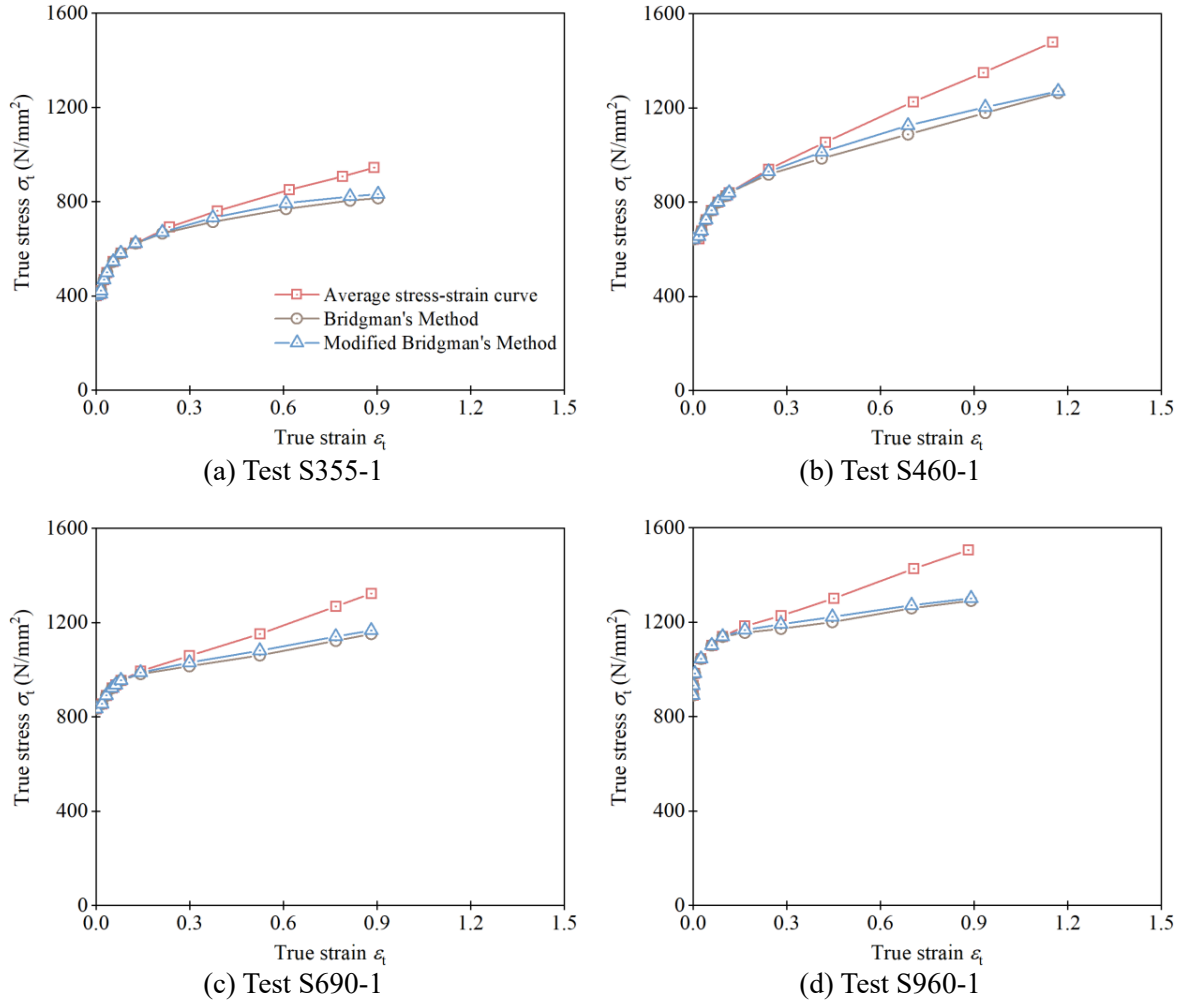


Figure 3.16 True stress-strain ( $\sigma_t - \varepsilon_t$ ) curve of S355 to S960 steels.

### 3.3.5. Finite element modelling of tensile tests

In order to simulate the structural behaviour of standardized steel coupons under tension, a finite element model is established, as shown in Figure 3.17, with the general finite element software Abaqus, and solid element C3D8R is adopted in the model. Due to symmetry, only one eighth of the coupon is modelled, and both its mesh and its boundary condition are illustrated in Figure 3.17a). The element sizes of the numerical models are determined based on the sensitivity study of mesh size. Four different mesh sizes are investigated in this study, i.e. 0.1, 0.2, 0.5 and 1 mm, and the predicted load extension (N- $\Delta$ ) curves predicted by numerical models are compared with test data in Figure 3.18. Based on the results of sensitivity study, the mesh size used in all the

numerical models ranges from 0.1 mm to 0.2 mm in their critical parts. For typical model adopted in the present study, as shown in [Figure 3.17](#), the diameter of the coupon is 5.0 mm, and hence, typical size of the solid elements at mid-length of the coupon is 0.2 x 0.2 x 0.05 (L) mm while it increases gradually to 0.2 x 0.2 x 1.0 (L) mm towards both ends of the model, as shown in [Figure 3.17a](#)). It should be noted that during the numerical simulation, those elements at the mid-length of the coupon are elongated to about 0.2 to 0.4 mm at various deformation stages as shown in [Figure 3.17b](#)). Hence, this numerical model is demonstrated to be able to provide both load-extension curves and stress distributions with a high level of numerical accuracy [46, 78, 79]. It should be noted that all the nodes at the end of the model are constrained to move together with the reference point which deformation is under displacement control during geometrical and material non-linear analyses. For each steel, the following stress-strain curves are adopted in these models: i) average stress-strain curves, ii) Bridgman method, and iii) Modified Bridgman method. These predicted load-extension ( $N - \Delta$ ) curves are plotted in [Figure 3.19](#) together with the test data. It is evident that:

- The models with the average stress-strain ( $\sigma_{av} - \varepsilon_{av}$ ) curves always overpredict the resistances of coupons at large extensions after occurrence of necking.
- On the contrary, the models with the Bridgman method always underpredict the resistances of coupons at large extensions after occurrence of necking.
- Through the use of the Modified Bridgman method, the models are ready to predict the resistances of coupons at large extensions with a high degree of accuracy.

Hence, it is shown that the Modified Bridgman method is highly effective to provide an accurate prediction to both the load-extension ( $N - \Delta$ ) curves of the standard coupons with various steel grades. The proposed empirical expression to predict the spatial necking radii ratio  $a/R$  is also shown to be effective.

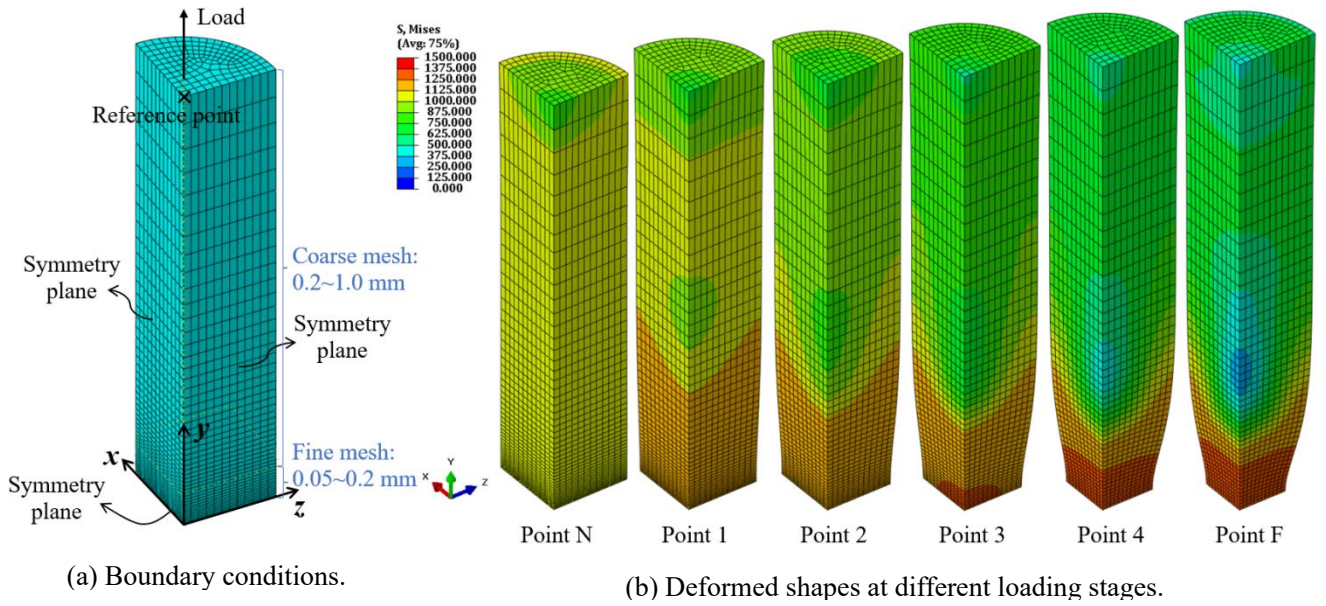


Figure 3.17 Finite element modelling for a standard coupon – Test S960-1.

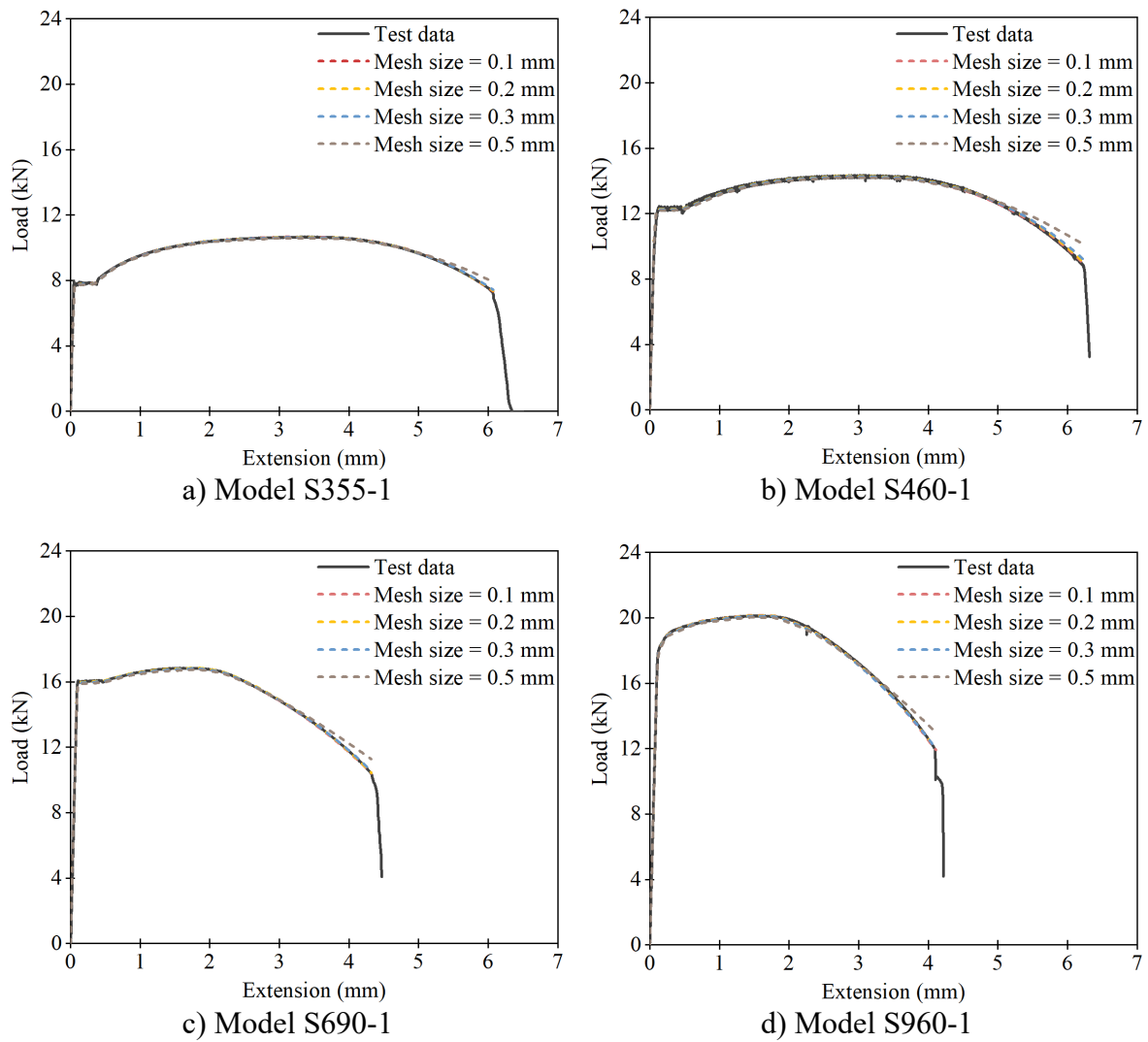


Figure 3.18. Sensitivity study of mesh size.

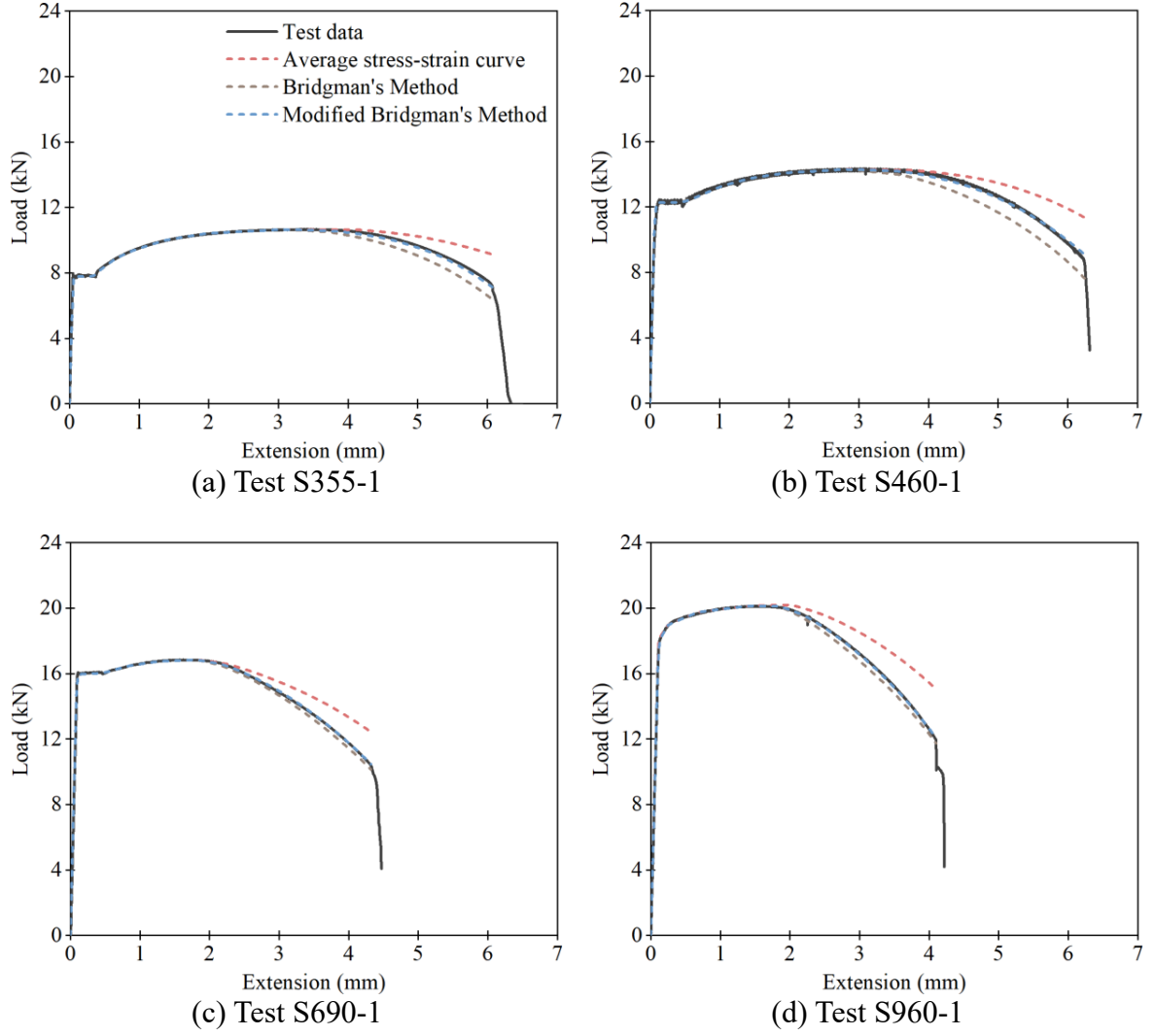


Figure 3.19 Load-extension ( $N - \Delta$ ) curves.

### 3.3.6. Comparison with various methods

Bridgman proposed a relationship between  $a/R$  ratio and average strain  $\varepsilon_{av}$  which was readily described as a linear curve at small to medium strain level (i.e. average strain  $\leq 1.2$ ), as shown in Figure 3.5. The  $a/R$  ratios were calculated by Eq. 14 in which the values of  $c_\omega$  were obtained after calibration against measured data of  $a$  and  $R$ , as shown in Figure 3.16. All the values of  $c_\omega$  and  $\varepsilon_n$  are summarized in Table 3-5.

The stress correction factors of Bridgman's Method,  $\eta_\sigma^B$ , are calculated according to Eqs. 13 and 14, and they are summarized in Table 3-6. The corrected true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves are plotted in Figure 3.16, which are used in subsequent numerical

simulations as shown in Figure 3.17. The predicted load-extension ( $N - \Delta$ ) curves based on the true stress-strain curves obtained using the Bridgman expressions are plotted in Figure 3.19. It shows that:

- a) the predicted load-extension ( $N - \Delta$ ) curves of these tensile tests based on the average stress-strain ( $\sigma_{av} - \varepsilon_{av}$ ) curves tend to overestimate the resistances at large deformations after necking, when compared with measured data;
- b) the predicted load-extension ( $N - \Delta$ ) curves of these tensile tests based on the true stress-strain ( $\sigma_t - \varepsilon_t$ )<sub>B</sub> curves obtained with the Bridgman method, however, tend to under-estimate the resistances at large deformations after necking, when compared with measured data; and
- c) a good comparison is found on the predicted load-extension ( $N - \Delta$ ) curves of these tensile tests based on the true stress-strain ( $\sigma_t - \varepsilon_t$ )<sub>MB</sub> curves obtained with the proposed Modified Bridgman method with measured data at large deformations after necking.

**Table 3-6** Correction factors in Bridgman and Modified Bridgman methods.

| Camera captured point | $\eta_{\sigma}^B$ (1954) | $\eta_{\sigma}^{MB}$ (2024) |
|-----------------------|--------------------------|-----------------------------|
| Test S355-1           |                          |                             |
| Point N               | 1.000                    | 1.000                       |
| Point 1               | 0.983                    | 0.989                       |
| Point 2               | 0.953                    | 0.970                       |
| Point 3               | 0.914                    | 0.944                       |
| Point 4               | 0.884                    | 0.906                       |
| Point F               | 0.872                    | 0.892                       |
| Test S460-1           |                          |                             |
| Point N               | 1.000                    | 1.000                       |
| Point 1               | 0.976                    | 0.989                       |
| Point 2               | 0.945                    | 0.971                       |
| Point 3               | 0.901                    | 0.933                       |
| Point 4               | 0.868                    | 0.885                       |
| Point F               | 0.839                    | 0.859                       |
| Test S690-1           |                          |                             |
| Point N               | 1.000                    | 1.000                       |
| Point 1               | 0.988                    | 0.994                       |
| Point 2               | 0.958                    | 0.973                       |
| Point 3               | 0.922                    | 0.939                       |
| Point 4               | 0.885                    | 0.899                       |
| Point F               | 0.870                    | 0.882                       |
| Test S960-1           |                          |                             |
| Point N               | 1.000                    | 1.000                       |
| Point 1               | 0.976                    | 0.986                       |
| Point 2               | 0.955                    | 0.971                       |
| Point 3               | 0.927                    | 0.944                       |
| Point 4               | 0.889                    | 0.898                       |
| Point F               | 0.866                    | 0.873                       |

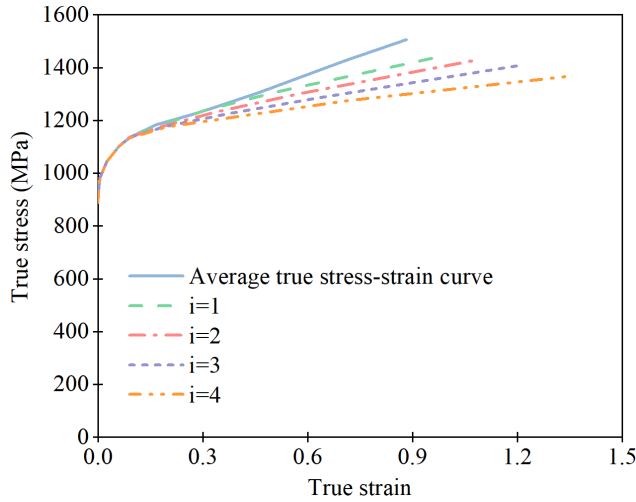


### 3.4. Comparison with numerical results of Instantaneous Area Method

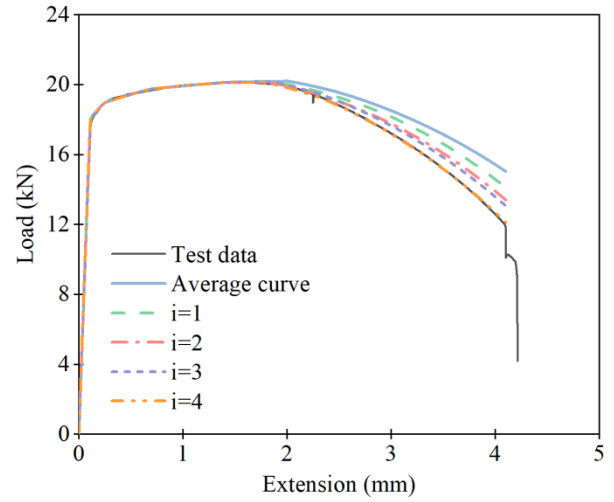
It is considered to be worthwhile to compare the numerical results of the Instantaneous Area Method, or *iAreaM*, proposed by the authors [14, 15] as described in Section 2.5. The finite element model shown in Figure 3.17 is also adopted for successive approximations in *iAreaM* for various steel. For ease of presentation, the true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves and the load-extension ( $N - \Delta$ ) curves of Test S960-1 during successive approximations are plotted together in the same graph of Figure 3.19 respectively to allow for a direct comparison. It is shown that after four iterations, convergence is achieved in these two curves. Figure 3.20 plots the development of non-uniform stresses and strains at the critical cross-sections of Coupon S960-1 at large deformations after necking, and it is apparent that these variations for both the stresses and the strains are significant. Hence, the effects of non-uniform stresses and strains at the critical cross-sections of the coupons after onset of necking should be incorporated carefully during structural analyses at large deformations. Figure 3.21 plots the converged true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves of various steel, and the corresponding correction factors are summarized in Table 3-6. Figure 3.22 plots the predicted load-extension ( $N - \Delta$ ) curves of various steel coupons by *iAreaM* together with test data. It is evident that a good agreement is achieved not only in the initial deformations but also at the large deformations of these coupons.

**Table 3-7** Correction factors in Instantaneous Area Method (*iAreaM*).

| Camera<br>captured<br>point | Test S355-1          |                 | Test S460-1          |                 | Test S690-1          |                 | Test S960-1          |                 |
|-----------------------------|----------------------|-----------------|----------------------|-----------------|----------------------|-----------------|----------------------|-----------------|
|                             | $\eta_\varepsilon^I$ | $\eta_\sigma^I$ | $\eta_\varepsilon^I$ | $\eta_\sigma^I$ | $\eta_\varepsilon^I$ | $\eta_\sigma^I$ | $\eta_\varepsilon^I$ | $\eta_\sigma^I$ |
| Point N                     | 1.001                | 1.000           | 1.000                | 1.000           | 0.999                | 1.000           | 0.940                | 0.991           |
| Point 1                     | 1.016                | 0.998           | 1.008                | 0.993           | 1.000                | 0.995           | 1.150                | 0.990           |
| Point 2                     | 1.021                | 0.981           | 1.021                | 0.983           | 1.074                | 0.965           | 1.444                | 0.981           |
| Point 3                     | 1.074                | 0.946           | 1.047                | 0.946           | 1.140                | 0.941           | 1.542                | 0.967           |
| Point 4                     | 1.144                | 0.891           | 1.089                | 0.909           | 1.193                | 0.916           | 1.534                | 0.927           |
| Point F                     | 1.204                | 0.885           | 1.126                | 0.880           | 1.250                | 0.901           | 1.509                | 0.914           |

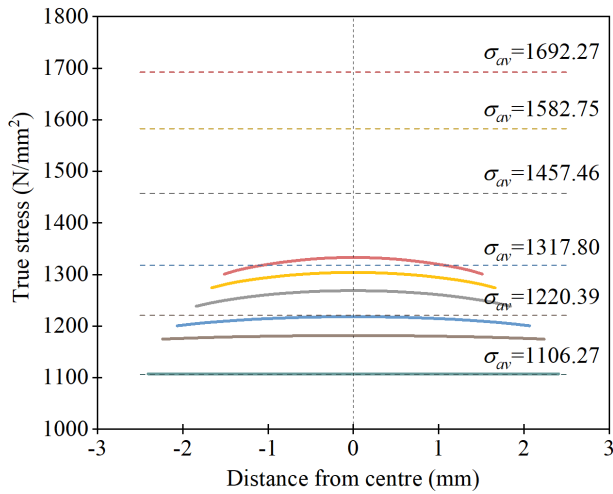


(a) True stress-strain curves

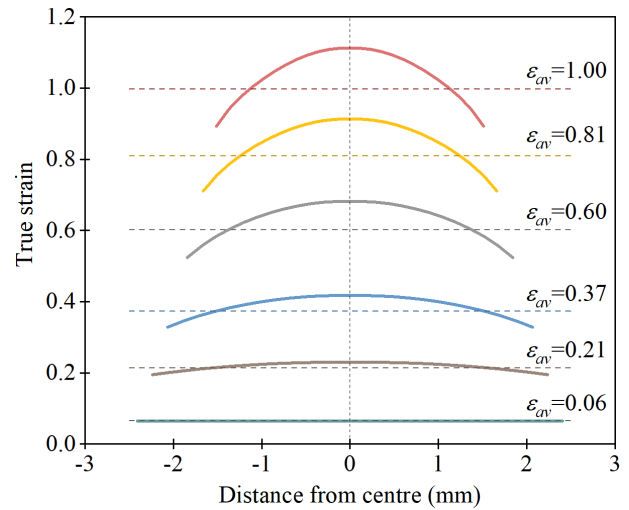


(b) Predicted load-extension curves

Figure 3.20 Successive approximations in iAreaM for Coupon S960-1.

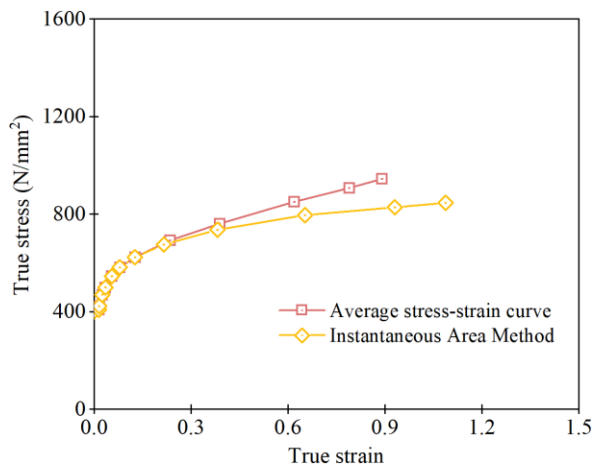


a) True stress

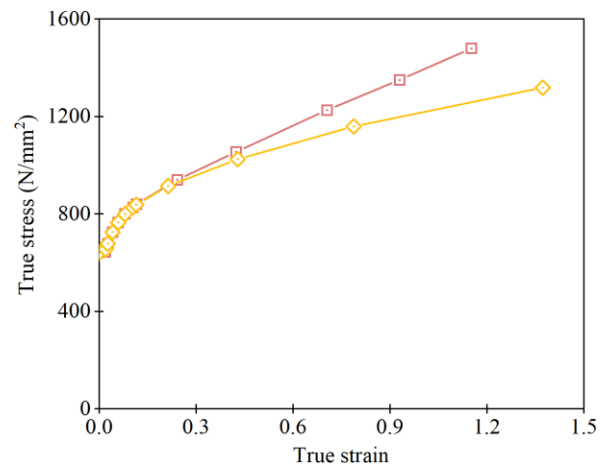


b) True strain

Figure 3.21 Distributions of true stresses and strains across the critical cross section of Coupon S960-1.



a) Test S355-1



b) Test S460-1

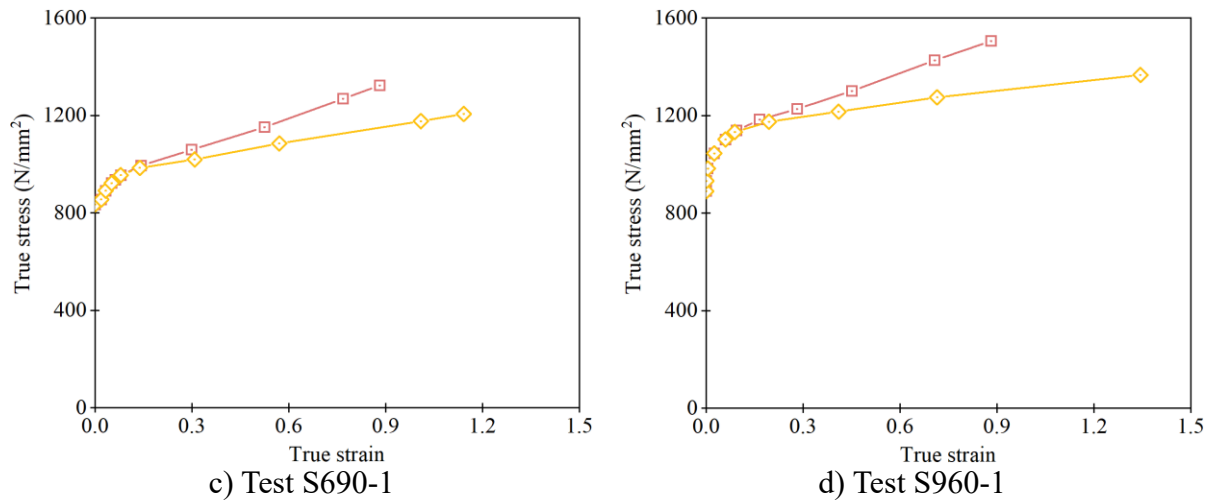


Figure 3.22 True stress-strain ( $\sigma_t - \epsilon_t$ ) curve of S355 to S960 steels corrected by *iAreaM*.

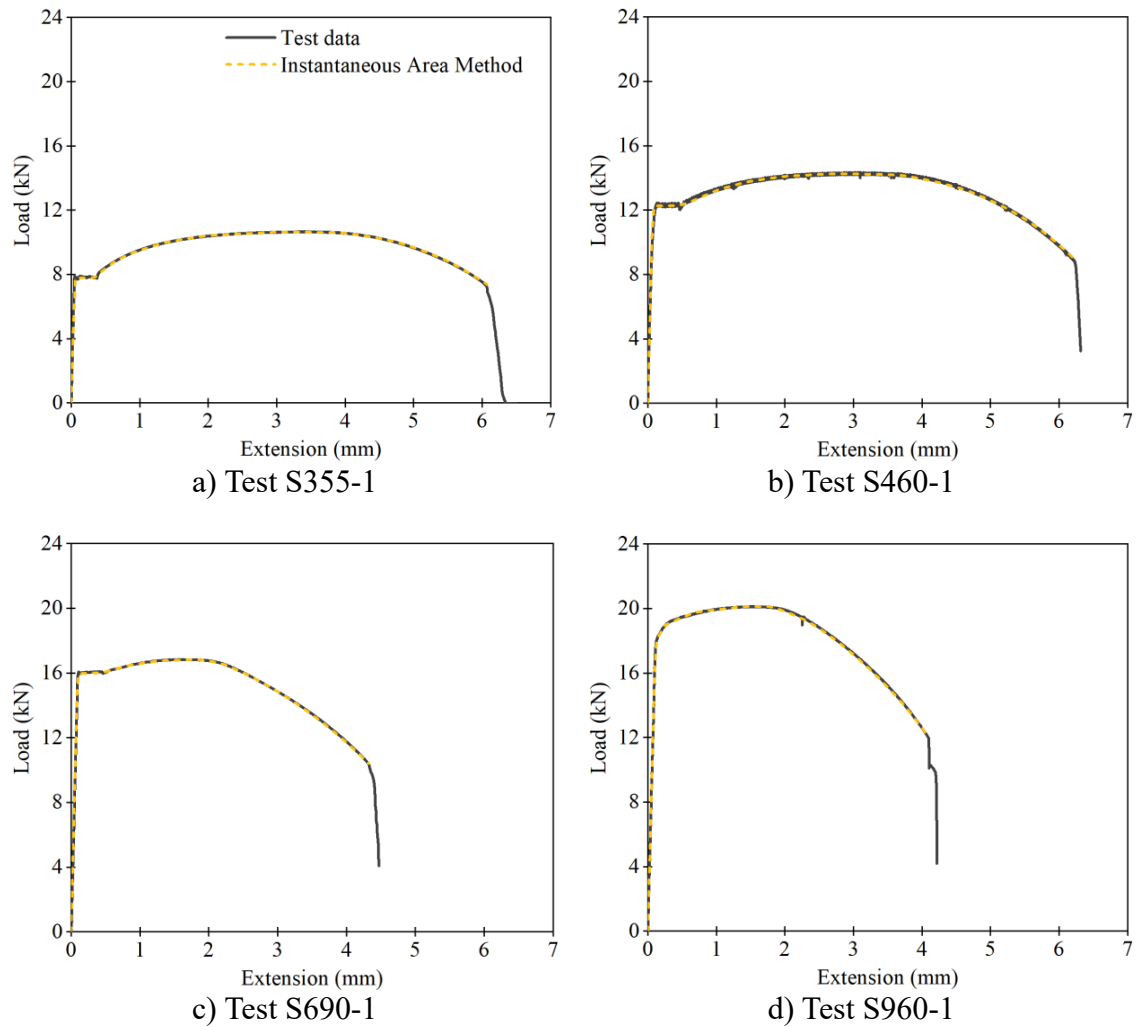


Figure 3.23 Load-extension ( $N - \Delta$ ) curves by *iAreaM*.

### 3.5. Conclusions

This chapter presents a comprehensive investigation into the true stress-strain relationships of S355 to S960 structural steels using experimental and numerical approaches. Standard tensile tests were conducted on cylindrical standard coupons of four steel grades, and advanced digital imaging techniques were employed to capture precise geometric parameters,  $a$  and  $R$ , of the necking region. A Modified Bridgman Method was proposed to simplify the determination of the spatial necking radii ratio  $a/R$ , and its accuracy was validated through finite element simulations, and also comparisons with test data. In addition, a comparison with numerical data obtained from Instantaneous Area Method (*iAreaM*) is found to be highly satisfactory. Key findings are summarized as follows:

- 1) A cubic function was derived to describe the relationship between the spatial necking radii ratio  $a/R$  and the average strain  $\varepsilon_{av}$ . This formula eliminated the need for laborious measurements of  $R$ , and incorporated material parameters such as the necking strain  $\varepsilon_n$ . The formula was demonstrated to be applicable to all the steel covered in the current study.
- 2) Finite element simulations were performed to predict load-extension ( $N - \Delta$ ) curves for direct comparison with measured data. These simulations allowed assessment on accuracy of various true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves obtained both the Bridgman and the Modified Bridgman methods. The proposed Modified Bridgman Method was shown to outperform consistently the original Bridgman method, which underestimated true stresses. Moreover, it was able to provide accurate results comparable to the rigorous *iAreaM*, but with significantly reduced computational effort.
- 3) It is confirmed that the post-necking stress distributions at the critical cross-sections

of these coupons are governed by their average strains at necking, and these strains often have the same numerical values of the hardening exponent ( $n$ ) in the stress-strain constitutive models. This insight supports the universal applicability of the proposed  $a/R$  expression for structural steel of various grades.

In short, the proposed Modified Bridgman method is a robust and efficient tool for deriving true stress-strain ( $\sigma_t - \varepsilon_t$ ) curves of structural steel, enabling accurate simulations of their structural behaviour at large deformations after on-set of necking up to initiation of fracture.

## Chapter 4

# In-situ X-ray CT investigation on micro-void evolution of structural steel towards verification on micro-fracture mechanics

### 4.1 Introduction

This study aims to investigate the micro-void evolution in structural steel through the use of in-situ X-ray computed tomography (CT), and specially designed steel coupons were loaded in displacement control to various elongations, and scanned simultaneously to capture formation or growth of micro-voids with a minimum volume of  $56 \mu\text{m}^3$  up to fracture. Three CT coupons of different steel grades, i.e. S355, S690 and S960 steel, were investigated, to track key stages of micro-void evolution, i.e. *nucleation*, *growth*, and *coalescence*, through scanning and imaging at critical deformation points. High-resolution 3D reconstruction technique was then employed to conduct detailed morphological analysis of the micro-voids captured, and then, their sizes, locations and distributions in the vicinity of the necking region of the coupons were examined. Then, advanced numerical models were established to examine the influence of states of ‘stress and strain’, i.e. stress triaxiality  $T$  and equivalent plastic strain  $\varepsilon_{\text{eq}}$ , on the evolution of micro-voids, and the classical Rice-Tracey formula was verified according to the states of ‘stress and strain’ obtained from the numerical models and the measured volume changes of single voids.

The investigation takes the form of the following tasks:

- 1) To observe and quantify nucleation, growth, and coalescence of micro-voids in structural steel during tensile tests through the use of in-situ X-ray CT technique,

and to compare the entire micro-void evolution processes in three CT coupons of different steel grades, i.e. S355, S690, and S960 steel, at various deformation points.

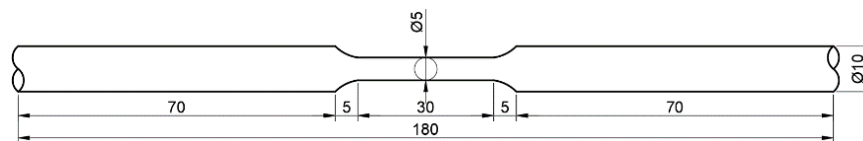
- 2) To integrate experimental measurements with numerical simulations on sizes and locations of micro-voids to establish effects of stress triaxiality and equivalent plastic strains onto micro-void evolution.
- 3) To verify various micro-void parameters, such as void growth rate and volume fraction, of classical ductile fracture models, i.e. VGM and GTN models.

Key areas of interest are:

- The micro-void evolution processes in the coupons during the entire deformation ranges up to fracture.
- Exact sizes, locations and distributions of the micro-voids just before fracture of the coupons.
- The relationship between stress triaxiality and equivalent plastic strain, and the micro-void evolution of the coupons.

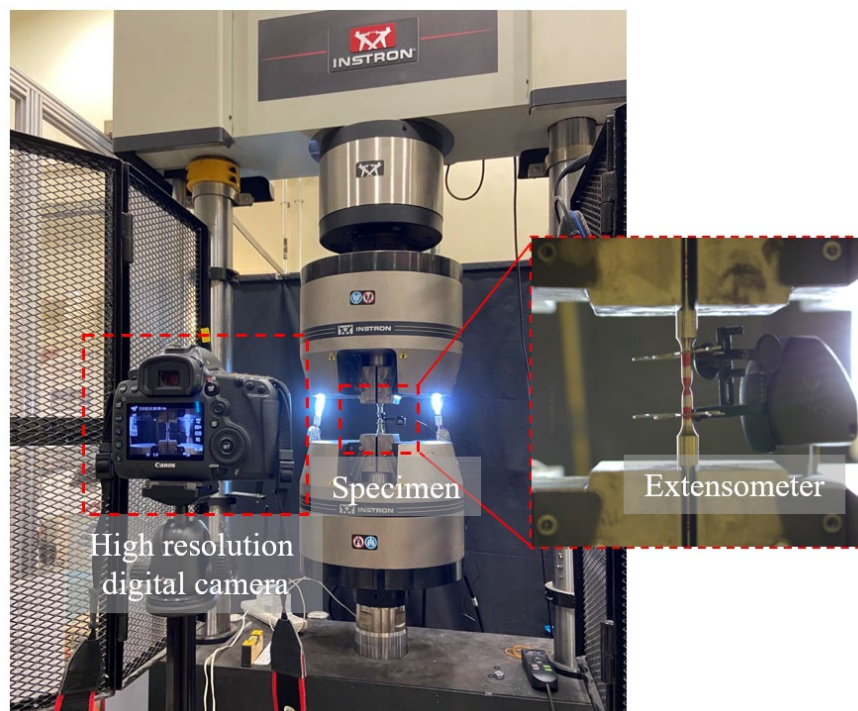
#### 4.1.1 Standard tensile tests

Standard tensile tests were carried out to provide basic mechanical properties of the structural steel, and for each of the steel grades of S355, S690 and S960 steel, a proportional cylindrical coupon was tested under monotonic tensile action. Typical dimensions of the proportional coupons are shown in **Figure 4.1**. The nominal diameter of the parallel region of the coupons is 5.0 mm, and their gauge lengths is 25.0 mm, which meet the requirements of BS EN ISO 6892-1:2019 [75].



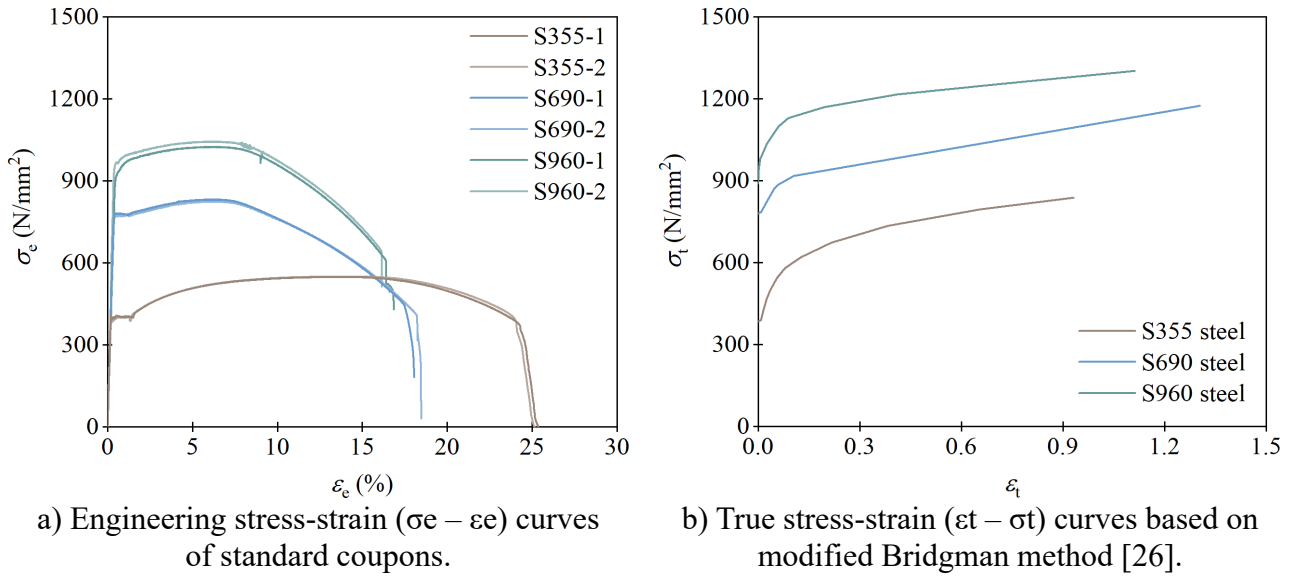
**Figure 4.1** Typical dimensions of a standard coupon for tensile tests (Unit: mm).

All the standard tensile tests were carried out with a high precision testing system of INSTRON 8803. Both an extensometer and a high-resolution digital camera were adopted to capture deformations of the coupons during the experiments. It should be noted that high resolution digital images of the coupons were taken every 30 seconds to capture the deformed shapes of the coupons. The test set-up is illustrated in [Figure 4.2](#). All the tests were conducted under a displacement control. The loading rate at the linear stage was set at 0.05 mm/min, and it was gradually increased to 0.1 mm/min when the measured deformations became non-linear. After a peak load was reached, the loading rate was increased to 0.2 mm/min. The measured engineering stress-strain curves for the three steel are plotted in [Figure 4.3a](#)), and key mechanical properties are listed in [Table 4-1](#). These data are adopted in subsequent finite element modelling of the CT coupons as described in [Chapter 4.3](#).



[Figure 4.2](#) Test setup for standard tensile tests.





**Figure 4.3** Stress-strain curves of standard tensile tests.

**Table 4-1** Mechanical properties of S355 to S960 steel.

| Steel grade | Young's modulus<br>$E$ (kN/mm <sup>2</sup> ) | Yield strength<br>$f_y$ (N/mm <sup>2</sup> ) | Tensile strength<br>$f_u$ (N/mm <sup>2</sup> ) | Strain at $f_u$<br>$\epsilon_u$ (%) | Strain at fracture<br>$\epsilon_L$ (%) |
|-------------|----------------------------------------------|----------------------------------------------|------------------------------------------------|-------------------------------------|----------------------------------------|
| S355        | 209                                          | 395                                          | 550                                            | 13.5                                | 24.3                                   |
| S690        | 218                                          | 777                                          | 833                                            | 6.1                                 | 18.1                                   |
| S960        | 206                                          | 944                                          | 1034                                           | 6.6                                 | 16.4                                   |

## 4.2 Experimental investigation

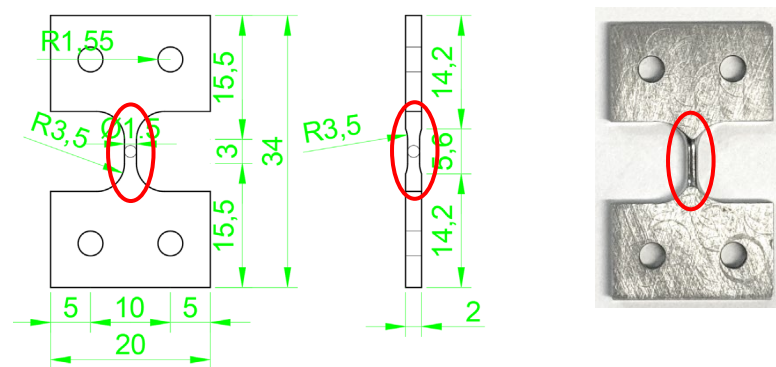
A total of three pairs of specially designed coupons for in-situ X-ray computed tomography were prepared, and they were scanned with X-ray intermittently at various deformation points during tensile tests. These tests provide important data on both micro-voids evolution and their corresponding ‘stress-strain’ conditions. The test programme is summarized in **Table 4-2**.

**Table 4-2** Test programme for S355 to S960 coupons.

| Steel grade | Proportional coupons   | Specially designed CT coupons                              |                                                   |
|-------------|------------------------|------------------------------------------------------------|---------------------------------------------------|
|             | Standard tensile tests | Pre-tests<br>- <i>No scanning</i> of coupons under loading | Full tests<br>- Scanning of coupons under loading |
| S355        | 2                      | 1                                                          | 1                                                 |
| S690        | 2                      | 1                                                          | 1                                                 |
| S960        | 2                      | 1                                                          | 1                                                 |

#### 4.2.1 Scanning of coupons under tension

All the coupons for X-ray CT scanning and imaging measurements are fabricated in a dog-bone shape, and typical dimensions of the CT coupons are illustrated in **Figure 4.4**. At both ends of the coupons, they are uniform in thickness, and two holes are provided to facilitate clamping during installation, and then load transfer during testing. The central parts of the coupons are cylindrical with circular cross-sections, and they are scanned with X-ray as the regions of interest (ROI). These parts are 1.5 mm in radius and 3 mm in length. Their surfaces are polished to mirror face to reduce influences of surface defects on the test results.



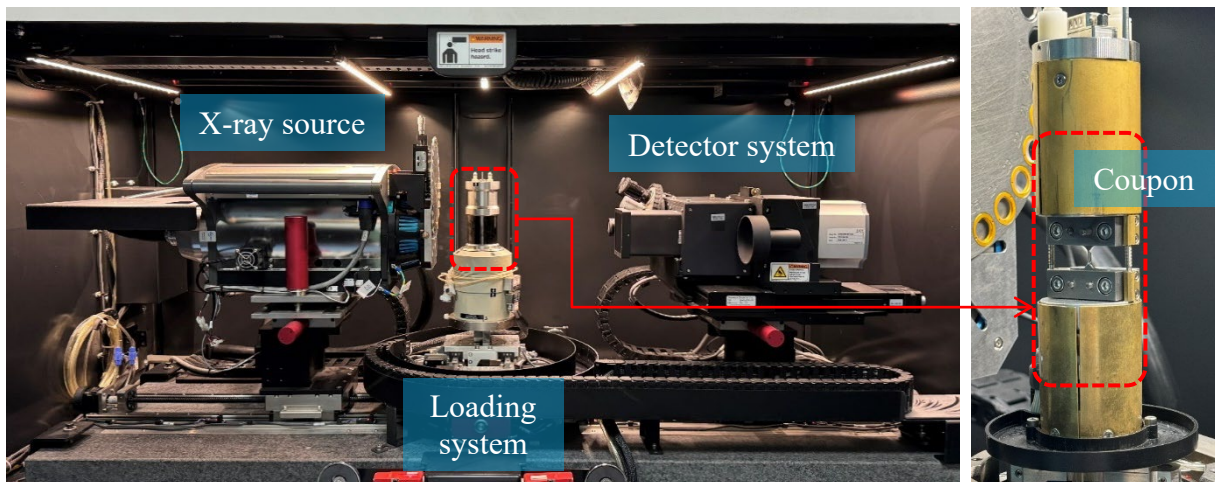
**Figure 4.4** Typical configuration of a CT coupon. Diameter,  $d = 1.5$  mm (Unit: mm).

The tensile tests with in-situ X-ray CT scanning were performed using a Zeiss Xradia 520 Versa X-ray microscope, as shown in **Figure 4.5a**). During the tests, the X-ray

source was set to a voltage of 160 kV with an output power of 10 W. The imaging area was  $3 \times 3$  mm, which allowed a resolution of  $3.028 \mu\text{m}$ . It should be noted that as a key feature of these tests, all the coupons were scanned while they were loaded in tension. The tensile action was applied using a versatile tension and compression testing device located inside the X-ray microscope chamber, as shown in **Figure 4.5b**). The loading rate in displacement control was set at 0.1 mm/min. Despite such a loading rate was rather large when compared with typical loading rates adopted in standard tensile tests, this was the lowest value allowed in the test device.



a) Overall view



b) Set-up for simultaneous scanning and loading

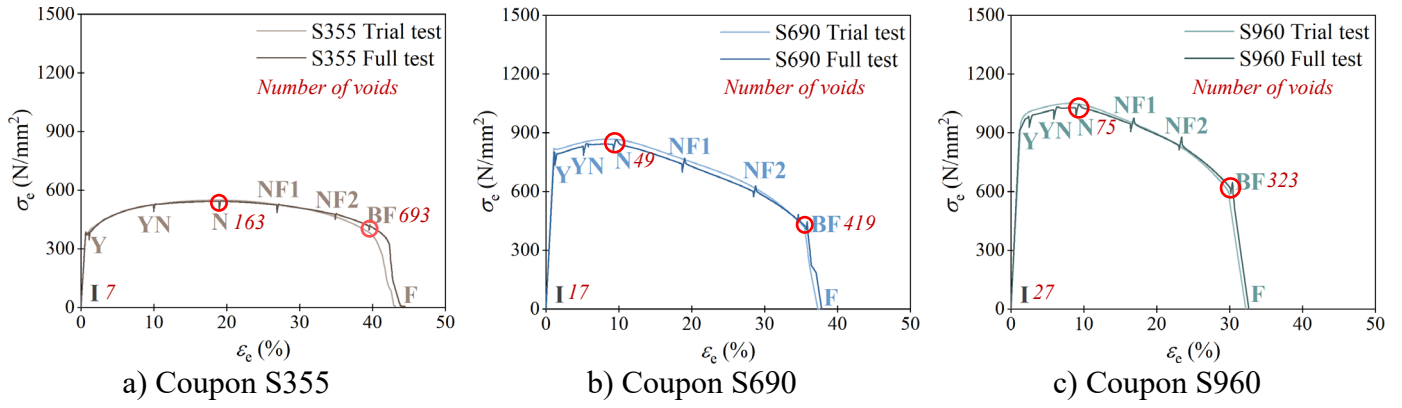
**Figure 4.5** High precision X-ray microtomography scanning and loading system.  
- Zeiss Xradia 520 Versa X-ray microscope.

In order to determine various deformation points for scanning of each pair of the CT coupons while they were loaded in tension, it was necessary to conduct i) a trial test on a coupon, and then, ii) a full test on the other coupon. In general, a total of eight deformation points were selected for scanning:

- i) the initial state before loading: Point I,
- ii) the yielding state: Point Y,
- iii) the pre-necking state: Point YN,
- iv) the onset of necking: Point N,
- v) the post-necking state: Point NF1,
- vi) the large deformation state at post-necking: Point NF2,
- vii) the point just before fracture, or the point of fracture initiation: Point BF, and
- viii) the point of fracture: Point F.

Hence, the measured load-extension ( $N - \Delta$ ) curves of both the trial and the full tests marked with various deformation points are plotted as shown in [Figure 4.6](#). For each CT scan, the coupon was rotated from  $0^\circ$  to  $180^\circ$ , and a total of 1,201 projection images were taken at an interval of  $0.15^\circ$ . As each image was exposed for 9 seconds, a complete scanning of the CT coupon through a total rotation of  $180^\circ$  took about 3 hours, and hence, the duration of a complete full test for the coupon was 24 hours. It should be noted that:

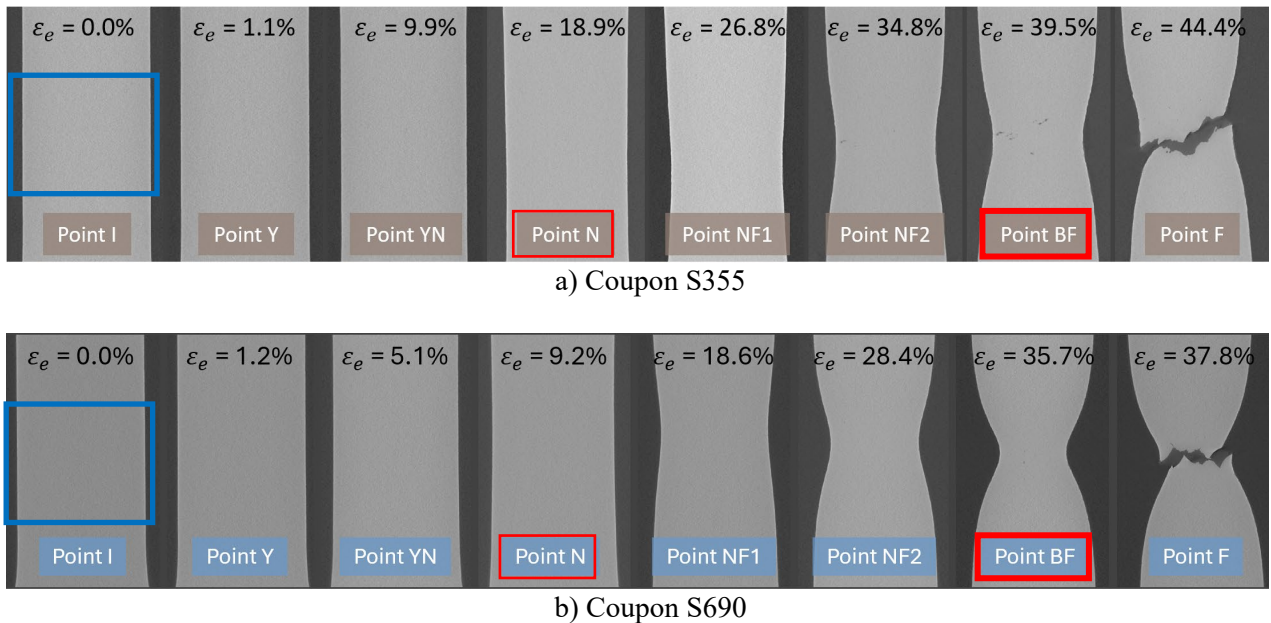
- For Coupon S355, Point BF may be considered as the point close to fracture initiation.
- For Coupons S690 and S960, Point BF should be considered as the *exact* point of fracture initiation, and their data are readily adopted to illustrate the deformations with the presence of a large amount of micro-voids.

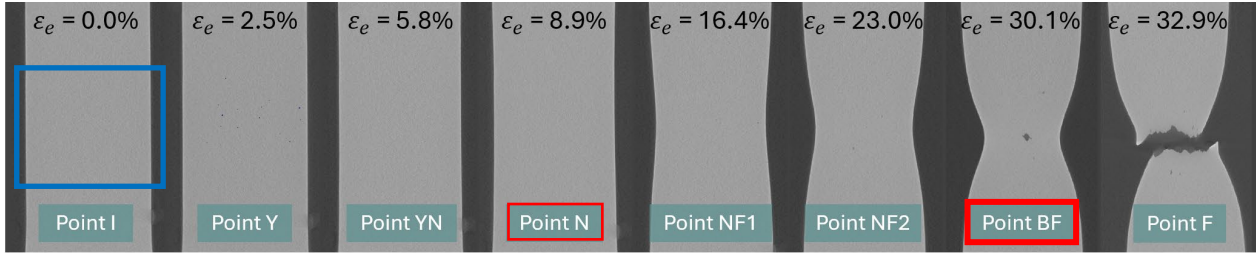


**Figure 4.6** Engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curves of the CT coupons.

#### 4.2.2 Data reconstruction and analysis

After data analysis, a series of 2D X-ray computed tomography images of the three CT coupons is shown in **Figure 4.7** at key deformation points throughout the tests. All coupons exhibit clearly a necking behaviour after reaching Point N, although the extents of necking vary among the coupons. Notably, at the Point BF, some micro-voids become clearly visible in the necking region through naked eyes. These images provide a simple and yet important overview on occurrence of micro-voids within the central parts of the coupons, i.e. with a height of 3.0 mm, at various post-necking deformation points of the coupons.





c) Coupon S960

**Figure 4.7** 2D X-ray tomography scanning images at different deformation points.

Advanced 3D reconstruction of all the scanned images of the coupons was performed using a specialist software *Avizo* to display the sizes, locations and distributions of all the micro-voids detected. As each voxel in the reconstructed image has a resolution of  $3.028 \mu\text{m}$ , the smallest possible volume for a single voxel to be detected is  $3.028^3 = 27 \mu\text{m}^3$ . To minimize effects of image noise, any micro-void smaller than twice of the smallest possible volume of a single voxel to be detected, i.e.  $\leq 56 \mu\text{m}^3$ , was ignored in the analysis. By using the software, the sizes, the locations and the distributions of the micro-voids were obtained. It should also be noted that the region of interest is built up with a total of 400 sets of images, each with a voxel resolution of  $3.028 \mu\text{m}$  after careful calibration on the measurement accuracy, and hence, the overall height of the region is  $(400 \times 3.028) \mu\text{m}$ , i.e.  $1.2112 \text{ mm}$ .

#### 4.2.2.1 Volume and area fractions

It is necessary to define both volume fractions and area fractions of the micro-voids within the region of interest which are obtained after data analysis on the 3D distributions of the micro-voids. Based on the measurements, the volume fraction is given by:

$$\text{Volume fraction} = \frac{V_{\text{void}}}{V_{\text{region}}} \quad (4-1)$$



where  $V_{\text{void}}$  is the total volume of micro-voids detected within the region of interest, and  $V_{\text{region}}$  is the volume of the region of interest.

For each layer of the voxels within the region of interest, the corresponding area fraction is given by:

$$\text{Area fraction} = \frac{A_{\text{void}}}{A_{\text{region}}} \quad (4-2)$$

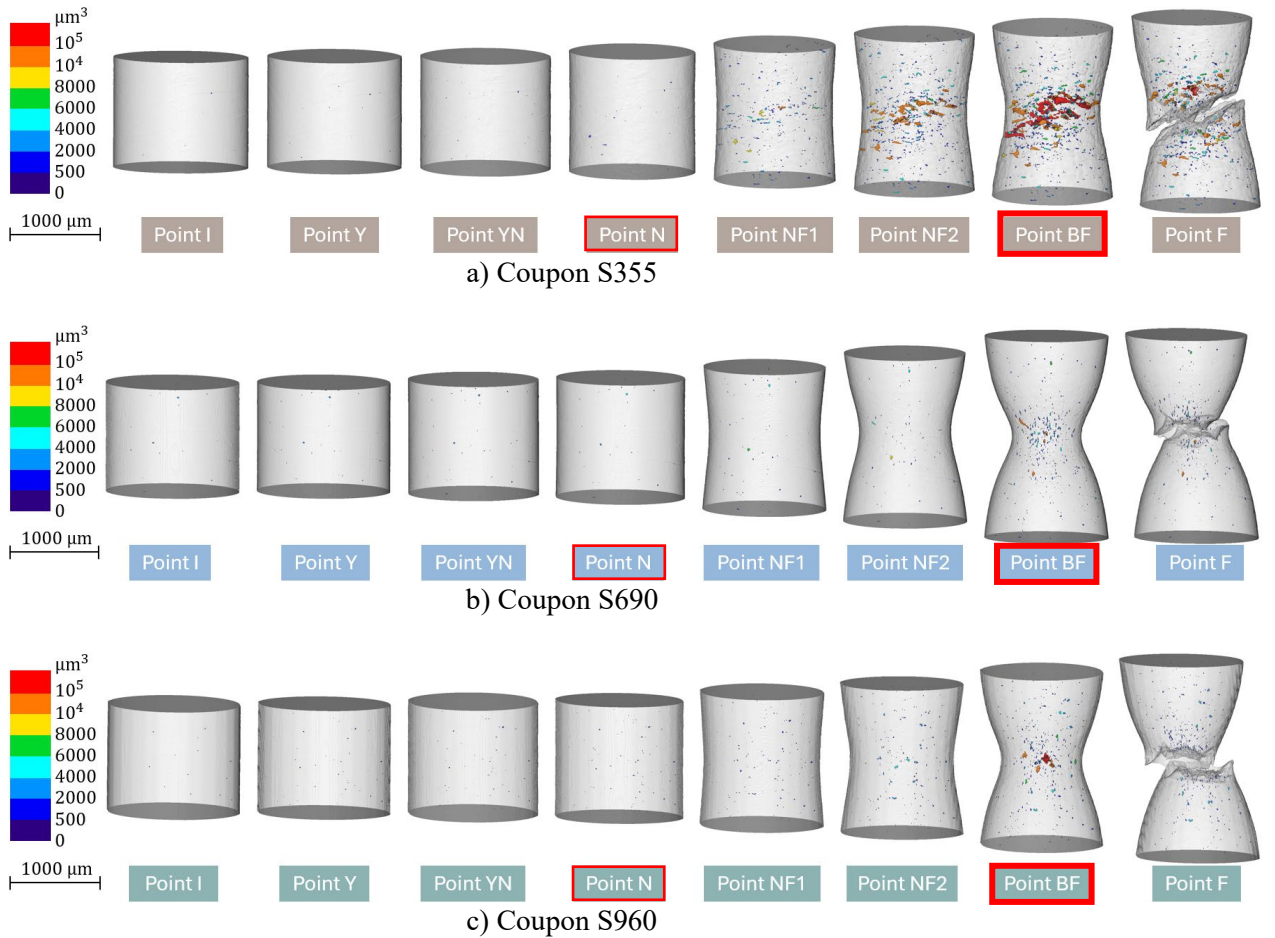
where  $A_{\text{void}}$  and  $A_{\text{region}}$  are the total cross-sectional area of micro-voids detected and the corresponding cross-sectional area of the voxel within the region of interest respectively.

#### 4.2.2.2 3D void distributions

A series of 3D X-ray CT images of the three CT coupons is shown in [Figure 4.8](#) at key deformation points throughout the tests. It is shown that certain micro-voids are detected in the central parts of the coupons, which are also the necking regions of the coupons. Hence, a portion of the central part of each of the coupons, with an initial height of 1.2112 mm is examined in details. [Figure 4.8](#) illustrates the 3D spatial distributions of all the micro-voids detected within the regions of interest of the three coupons at various deformation points. The micro-voids are coloured from purple, i.e. 0, to red, i.e.  $10^5 \mu\text{m}^3$ , or  $(0.0464 \text{ mm})^3$  based on their void sizes. Refer to the legend of colour for various ranges of sizes. As shown in [Figure 4.8](#), it is found that:

- For Coupon S355, micro-voids with increasing numbers and sizes are detected at various deformation points, and it is evident that most of these voids with a volume larger than  $10^5 \mu\text{m}^3$ , i.e. voids in red colour, are found in the vicinity of the minimum cross-section at Point BF. It should be noted that after fracture of the coupon at Point F, many of these micro-voids become parts of the fracture surface while the volumes of the remaining voids are reduced significantly.

- For Coupons S690 and S960, micro-voids with increasing numbers and sizes are also detected at various deformation points. However, it should be noted that both the numbers and sizes of micro-voids are significantly smaller than those of Coupon S355 in general. It is also evident that most of these voids with a volume larger than  $10^4 \mu\text{m}^3$ , i.e. voids in orange colour, are found in the vicinity of the minimum cross-section at Point BF.



**Figure 4.8** 3D spatial distributions of micro-voids within regions of interest at various deformation points.

A quantitative analysis on the micro-voids within the regions of interest of the CT coupons was performed, and **Table 4-3** summarizes data on the number of voids detected, and their corresponding volume at various deformation points. A close examination on **Table 4-3** reveals that:



- In general, micro-voids are detected in all these three CT coupons before loading at Point I, i.e. at the initial condition before testing, and the numbers of micro-voids of Coupons S355, S690 and S960 are found to be 7, 17 and 27 respectively.
- After a uniform deformation up to the onset of necking, i.e. Point N, new micro-voids have been formed almost randomly within the regions of interest of the coupons while existing voids are found to expand progressively. The numbers of micro-voids detected in Coupons S355, S690 and S960 are found to be 163, 49 and 75 respectively, and the corresponding volume fractions of the micro-voids are 0.0005, 0.0006 and 0.0009% respectively.
- After the onset of necking, i.e. from Point N to Point BF, a remarkable micro-void evolution is evident, and the numbers of micro-voids increase progressively to 693, 419 and 323 respectively, and the corresponding volume fractions of the micro-voids are 0.1526, 0.0127 and 0.0276% respectively. It is also evident that the majority of these micro-voids are formed in the vicinity of the minimum cross-sections of the coupons. The number of voids at Points I, N and BF are also marked in the engineering stress-strain curves of the coupons in [Figure 4.6](#) to provide a direct comparison on the process of micro-void evolution within the coupons with that of ‘macro-elongation’ of the coupons.
- At Point BF, the void volume fractions of Coupons S355, S690 and S960 are shown to be rather limited, i.e.  $\leq 0.2\%$ , and hence, it is difficult to relate these directly to fracture in the coupons, especially for Coupons S690 and S960.

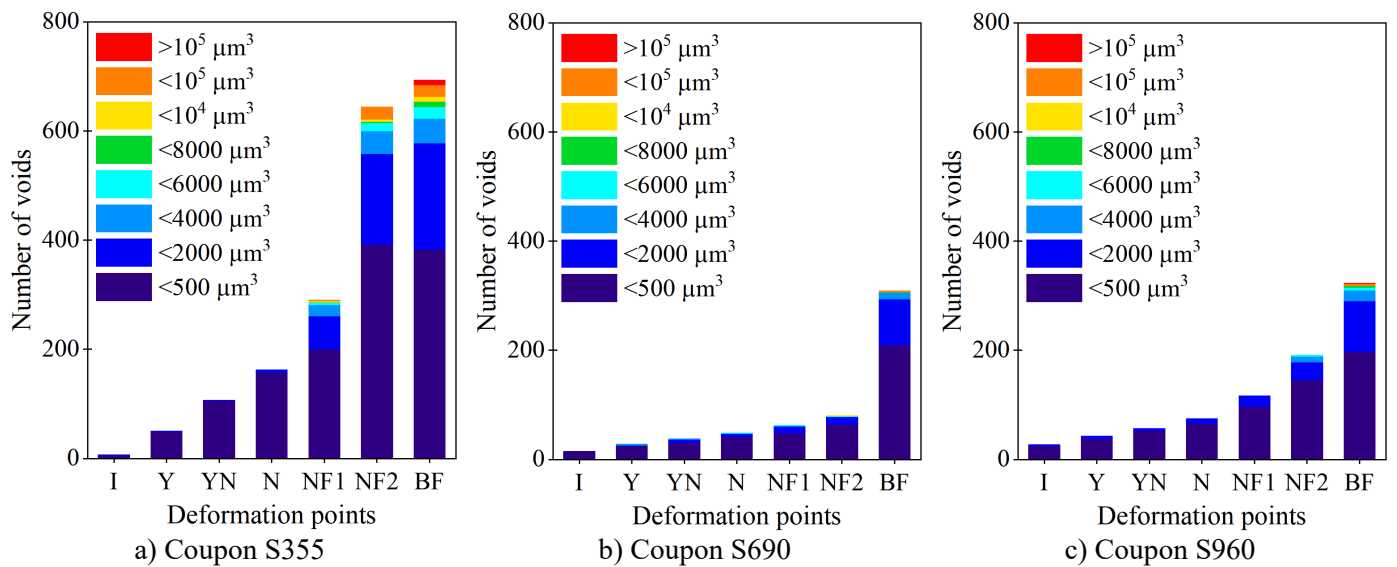
**Table 4-3** Quantitative analysis of micro-voids within the region of interest.

|             | Deformation points | $\varepsilon_e$ (%) | $A_i$ at critical cross section (mm <sup>2</sup> ) | $r_i$ at critical cross section (mm) | Number of voids | Volume fraction (%) | Void area fraction at critical cross section (%) | Maximum Void area fraction (%) |
|-------------|--------------------|---------------------|----------------------------------------------------|--------------------------------------|-----------------|---------------------|--------------------------------------------------|--------------------------------|
| Coupon S355 | Point I            | 0.0                 | 1.6655                                             | 0.7281                               | 7               | 0.0001              | 0.0000                                           | 0.0072                         |
|             | Point Y            | 1.1                 | 1.645                                              | 0.7236                               | 50              | 0.0001              | 0.0000                                           | 0.0137                         |
|             | Point YN           | 9.9                 | 1.5371                                             | 0.6995                               | 107             | 0.0002              | 0.0090                                           | 0.0168                         |
|             | Point N            | 18.9                | 1.4101                                             | 0.6700                               | 163             | 0.0005              | 0.0122                                           | 0.0182                         |
|             | Point NF1          | 26.8                | 1.1972                                             | 0.6173                               | 290             | 0.0112              | 0.0207                                           | 0.0883                         |
|             | Point NF2          | 34.8                | 0.948                                              | 0.5493                               | 644             | 0.0530              | 0.1451                                           | 0.3994                         |
|             | Point BF           | 39.5                | 0.7479                                             | 0.4879                               | 693             | 0.1526              | 0.4837                                           | 1.2222                         |
|             | Point F            | 44.4                | -                                                  | -                                    | -               | -                   | -                                                | -                              |
| Coupon S690 | Point I            | 0.0                 | 1.8575                                             | 0.7689                               | 17              | 0.0004              | 0.0000                                           | 0.0129                         |
|             | Point Y            | 1.2                 | 1.8413                                             | 0.7656                               | 28              | 0.0005              | 0.0000                                           | 0.0130                         |
|             | Point YN           | 5.1                 | 1.7687                                             | 0.7503                               | 38              | 0.0006              | 0.0000                                           | 0.0141                         |
|             | Point N            | 9.2                 | 1.6934                                             | 0.7342                               | 49              | 0.0006              | 0.0000                                           | 0.0147                         |
|             | Point NF1          | 18.6                | 1.3196                                             | 0.6481                               | 63              | 0.0011              | 0.0000                                           | 0.0325                         |
|             | Point NF2          | 28.4                | 0.8915                                             | 0.5327                               | 81              | 0.0019              | 0.0021                                           | 0.0446                         |
|             | Point BF           | 35.7                | 0.5052                                             | 0.4010                               | 419             | 0.0127              | 0.0983                                           | 0.2735                         |
|             | Point F            | 37.8                | -                                                  | -                                    | -               | -                   | -                                                | -                              |
| Coupon S960 | Point I            | 0                   | 1.7771                                             | 0.7521                               | 27              | 0.0003              | 0.0000                                           | 0.0088                         |
|             | Point Y            | 2.5                 | 1.7444                                             | 0.7452                               | 43              | 0.0005              | 0.0005                                           | 0.0121                         |
|             | Point YN           | 5.8                 | 1.6775                                             | 0.7307                               | 57              | 0.0006              | 0.0059                                           | 0.0184                         |
|             | Point N            | 8.9                 | 1.6269                                             | 0.7196                               | 75              | 0.0009              | 0.0066                                           | 0.0203                         |
|             | Point NF1          | 16.4                | 1.3472                                             | 0.6548                               | 117             | 0.0017              | 0.0070                                           | 0.0212                         |
|             | Point NF2          | 23.0                | 1.064                                              | 0.5820                               | 191             | 0.0048              | 0.0292                                           | 0.0571                         |
|             | Point BF           | 30.1                | 0.6416                                             | 0.4519                               | 323             | 0.0276              | 0.6321                                           | 0.8690                         |
|             | Point F            | 32.9                | -                                                  | -                                    | -               | -                   | -                                                | -                              |

#### 4.2.2.3 Distributions of void sizes

The distributions of void sizes in all these three CT coupons are plotted in **Figure 4.9**.

It should be noted that any micro-void with a volume less than  $500 \mu\text{m}^3$ , or  $(0.00793 \text{ mm})^3$ , is considered to be either small or newly formed voids, and they are coloured in purple. It is evident that the numbers of these small voids increase steadily as the applied deformations increase, indicating that void nucleation occurs throughout the tests. It is noted that the increases of the numbers of these small voids in Coupons S690 and S960 are far smaller than those of Coupon S355. Hence, Coupons S690 and S960 are demonstrated to have an enhanced resistance against void evolution, when compared with Coupon S355, owing to their high steel quality.



**Figure 4.9** Distributions of void sizes at various deformation points.

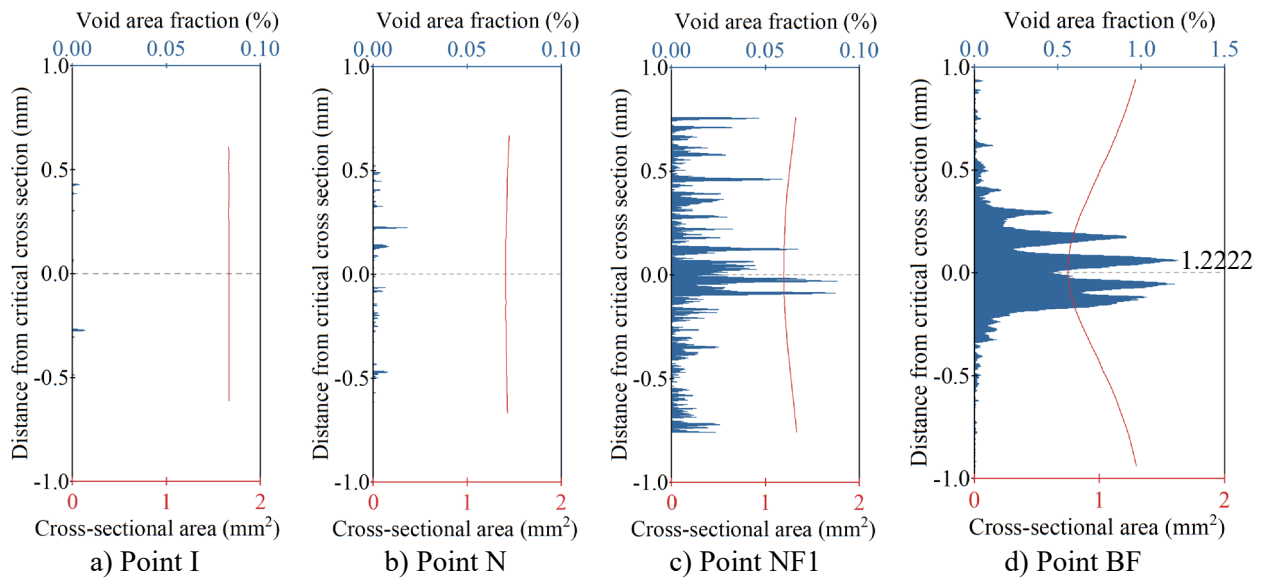
#### 4.2.2.4 Area fraction

As illustrated in [Figure 4.10](#) to [Figure 4.12](#), the development of void area fractions across various cross-sections of Coupons S355, S690 and S960 within the regions of interest is plotted at different deformation points, namely, Points, I, N, NF1 and BF. For each coupon, *the critical cross-section* prior to fracture is defined as the cross-section with the minimum cross-sectional area, and it is plotted as grey dashed lines along the y-axes in the graphs of the figures. The void area fractions along the regions of interests of these three coupons at the four different deformation points are drawn horizontally in blue to illustrate the micro-void distributions. Moreover, the longitudinal necking profiles of the regions of interest of the coupons, drawn in red, are plotted onto the graphs of various deformation points to provide a direct correlation between the micro-void evolutions and the overall necking development. It should be noted in general that:

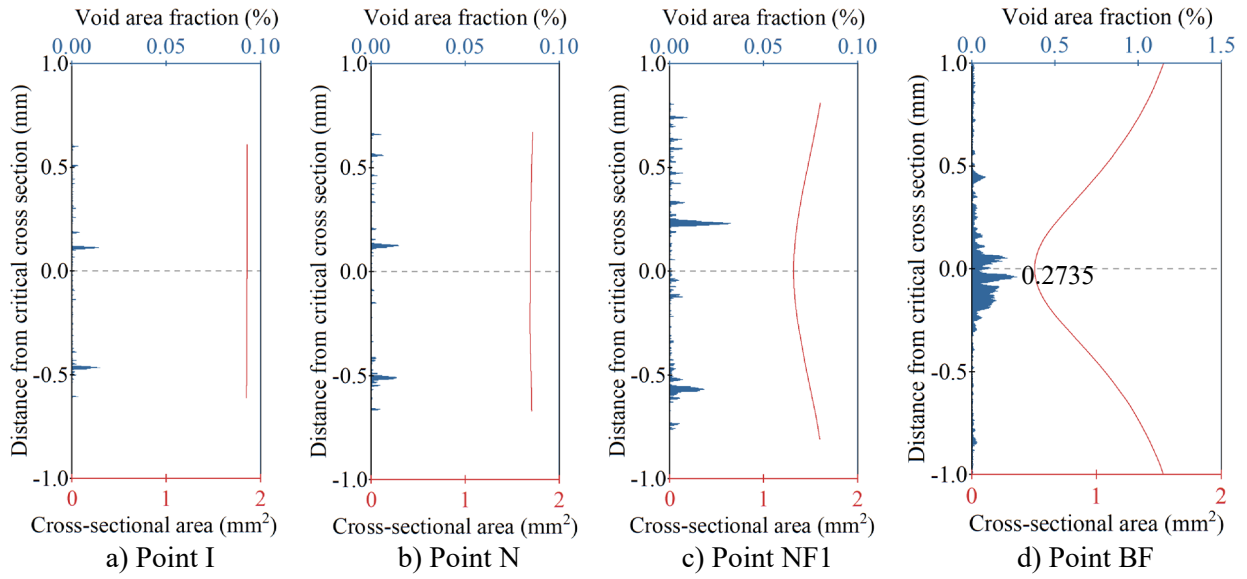
- In Point I, some micro-voids are detected in the initial stage, and they may be considered as pre-existing voids, i.e. initial defects of the steel matrix.
- In Point N, new micro-voids are formed in the vicinity of the necking regions while their sizes and locations are somehow random.
- In Point NF1, a large number of micro-voids are formed, and many of them grow in sizes by a factor of 5 to 10. They are formed randomly within the steel matrix, and their spatial distribution shows a limited correlation to the exact locations of eventual fracture. The maximum void area fractions do not happen exactly at the critical cross-sections, but rather exist in the close vicinity of the critical cross-sections.
- In Point BF, an enormous amount of micro-voids is steadily formed, and then, their sizes are progressively increased typically by a factor of 10 to 20. A detailed inspection on [Figure 4.8](#) reveals that only limited numbers of large micro-voids are

linked up to give ‘elongated’ micro-voids. It is very important to note that the cross-sectional areas of some of the largest micro-voids quantified by the X-ray CT technique are smaller than 1.0% of the cross-sectional areas of Coupons S690 and S960 even at the point of initiation of fracture. Hence, it is difficult to argue that these ‘elongated’ micro-voids are able to impose any dominant effect onto the overall fracture behaviour of these coupons.

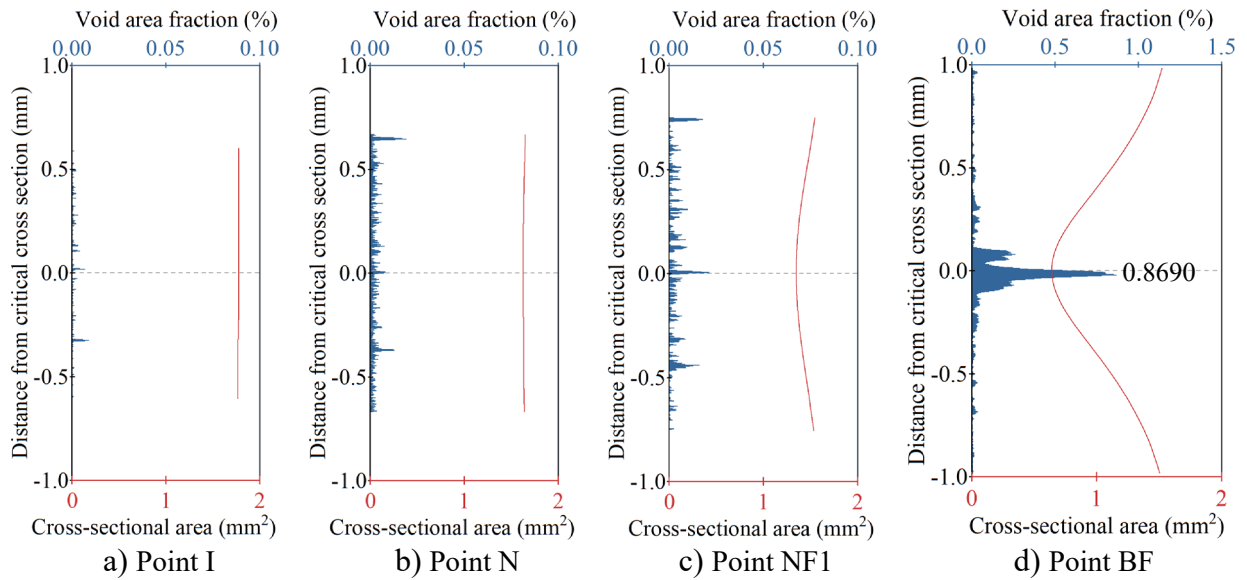
- Contrary to conventional assumptions, pre-existing voids do not pre-determine the exact locations of necking. Instead, it may be argued that the overall behaviour of *necking* in these coupons govern both void distributions and their subsequent increases in sizes under the effects of stress triaxiality. Refer to [Chapter 4.3](#) for details.



**Figure 4.10** Evolution of cross-sectional area and void area fractions in Coupon S355.



**Figure 4.11** Evolution of cross-sectional area and void area fractions in Coupon S690.



**Figure 4.12** Evolution of cross-sectional area and void area fractions in Coupon S960.

#### 4.2.2.5 Void evolution

For ease of discussion, three representative voids, namely, Voids  $V_1$ ,  $V_2$  and  $V_3$ , of Coupon S690 are selected for a detailed analysis of the micro-void evolution, and they are shown in **Figure 4.13** together with their progressive increases in sizes. Void  $V_1$  is the largest one among all voids detected in the initial state while Voids  $V_2$  and  $V_3$  are

randomly selected among those close to the fracture surfaces. For illustration purpose, a perspective cubic box with a length of 250  $\mu\text{m}$  per side is employed to define the spatial extent of the voids, and to indicate their locations within the regions of interest. It should be noted that some voids and cracks may span across a number of these cubic boxes, and only parts of these voids and cracks are shown. The sizes of Voids  $V_1$ ,  $V_2$  and  $V_3$  in different deformation points are summarized in [Table 4-4](#). It is found that:

- Under tension actions, most voids demonstrate pronounced volumetric expansion. The volumes of Voids  $V_1$ ,  $V_2$  and  $V_3$  increase from 2,706.1, 139.5 and 418.5  $\mu\text{m}^3$  at Point I, to 14,479.1, 4,547.4 and 10,406.0  $\mu\text{m}^3$  respectively at Point BF. In term of volumetric expansion, these voids expand in volume with a factor of roughly 5.4, 32.6 and 24.9 respectively.
- Despite Void  $V_1$  is the largest among the three in the initial state, its increase in size is rather limited, i.e. with an expansion factor of merely 5.4. However, for Voids  $V_2$  and  $V_3$ , their increases in size are very significant, and their expansion factors are 32.6 and 24.9 respectively. These differences are considered to be direct results of their relative positions to the fracture surface of the coupons, and hence, they are governed by highly localized ‘stresses and strains’, or the stress triaxiality, of the coupons.

Refer to [Chapter 4.3](#) for a detailed discussion on the stress triaxiality of the coupons.

**Table 4-4** Void sizes and stress states of selected voids at different deformation points.

| Deformation point | $\varepsilon_e$<br>(%) | Void V <sub>1</sub>               |                    |       | Void V <sub>2</sub>               |                    |       | Void V <sub>3</sub>               |                    |       |
|-------------------|------------------------|-----------------------------------|--------------------|-------|-----------------------------------|--------------------|-------|-----------------------------------|--------------------|-------|
|                   |                        | $V_{void}$<br>( $\mu\text{m}^3$ ) | $\varepsilon_{eq}$ | $T$   | $V_{void}$<br>( $\mu\text{m}^3$ ) | $\varepsilon_{eq}$ | $T$   | $V_{void}$<br>( $\mu\text{m}^3$ ) | $\varepsilon_{eq}$ | $T$   |
| Coupon S355       |                        |                                   |                    |       |                                   |                    |       |                                   |                    |       |
| Point I           | 0.0                    | 1,032.1                           | 0.000              | 0.333 | -                                 | -                  | -     | -                                 | -                  | -     |
| Point Y           | 1.1                    | 1,199.5                           | 0.009              | 0.331 | -                                 | -                  | -     | -                                 | -                  | -     |
| Point YN          | 9.9                    | 1,617.9                           | 0.075              | 0.333 | -                                 | -                  | -     | 83.7                              | 0.075              | 0.332 |
| Point N           | 18.9                   | 1,952.7                           | 0.126              | 0.338 | 585.8                             | 0.176              | 0.347 | 111.6                             | 0.183              | 0.362 |
| Point NF1         | 26.8                   | 2,092.2                           | 0.173              | 0.354 | 8,926.5                           | 0.301              | 0.368 | 2,678.0                           | 0.341              | 0.435 |
| Point NF2         | 34.8                   | 2,678.0                           | 0.219              | 0.351 | 20,921.5                          | 0.436              | 0.327 | 40,810.9                          | 0.624              | 0.558 |
| Point BF          | 39.5                   | 3,096.4                           | 0.233              | 0.328 | 24,771.0                          | 0.477              | 0.260 | 157,273.8                         | 0.837              | 0.640 |
| Coupon S690       |                        |                                   |                    |       |                                   |                    |       |                                   |                    |       |
| Point I           | 0.0                    | 2,706.1                           | 0.000              | -     | -                                 | -                  | -     | -                                 | -                  | -     |
| Point Y           | 1.2                    | 2,706.1                           | 0.003              | 0.343 | -                                 | -                  | -     | -                                 | -                  | -     |
| Point YN          | 5.1                    | 3,040.9                           | 0.043              | 0.323 | -                                 | -                  | -     | -                                 | -                  | -     |
| Point N           | 9.2                    | 3,403.6                           | 0.092              | 0.357 | 139.5                             | 0.093              | 0.364 | -                                 | -                  | -     |
| Point NF1         | 18.6                   | 7,197.7                           | 0.328              | 0.465 | 306.9                             | 0.348              | 0.504 | -                                 | -                  | -     |
| Point NF2         | 28.4                   | 9,652.7                           | 0.652              | 0.532 | 641.7                             | 0.755              | 0.645 | 418.5                             | 0.778              | 0.701 |
| Point BF          | 35.7                   | 14,479.1                          | 0.851              | 0.478 | 4,547.4                           | 1.168              | 0.763 | 10,406.0                          | 1.193              | 0.825 |
| Coupon S960       |                        |                                   |                    |       |                                   |                    |       |                                   |                    |       |
| Point I           | 0                      | 641.6                             | 0.000              | 0.333 | -                                 | -                  | -     | -                                 | -                  | -     |
| Point Y           | 2.5                    | 781.1                             | 0.013              | 0.339 | -                                 | -                  | -     | 83.7                              | 0.014              | 0.341 |
| Point YN          | 5.8                    | 864.8                             | 0.051              | 0.337 | 167.4                             | 0.051              | 0.333 | 111.6                             | 0.053              | 0.334 |
| Point N           | 8.9                    | 1,115.8                           | 0.086              | 0.347 | 362.6                             | 0.089              | 0.342 | 139.5                             | 0.091              | 0.367 |
| Point NF1         | 16.4                   | 1,617.9                           | 0.206              | 0.345 | 809.0                             | 0.275              | 0.421 | 613.7                             | 0.302              | 0.539 |
| Point NF2         | 23.0                   | 2,036.4                           | 0.275              | 0.250 | 2,873.2                           | 0.526              | 0.515 | 5,579.1                           | 0.584              | 0.685 |
| Point BF          | 30.1                   | 2,566.4                           | 0.287              | 0.011 | 31,633.3                          | 0.957              | 0.655 | 251,783.1                         | 1.049              | 0.865 |



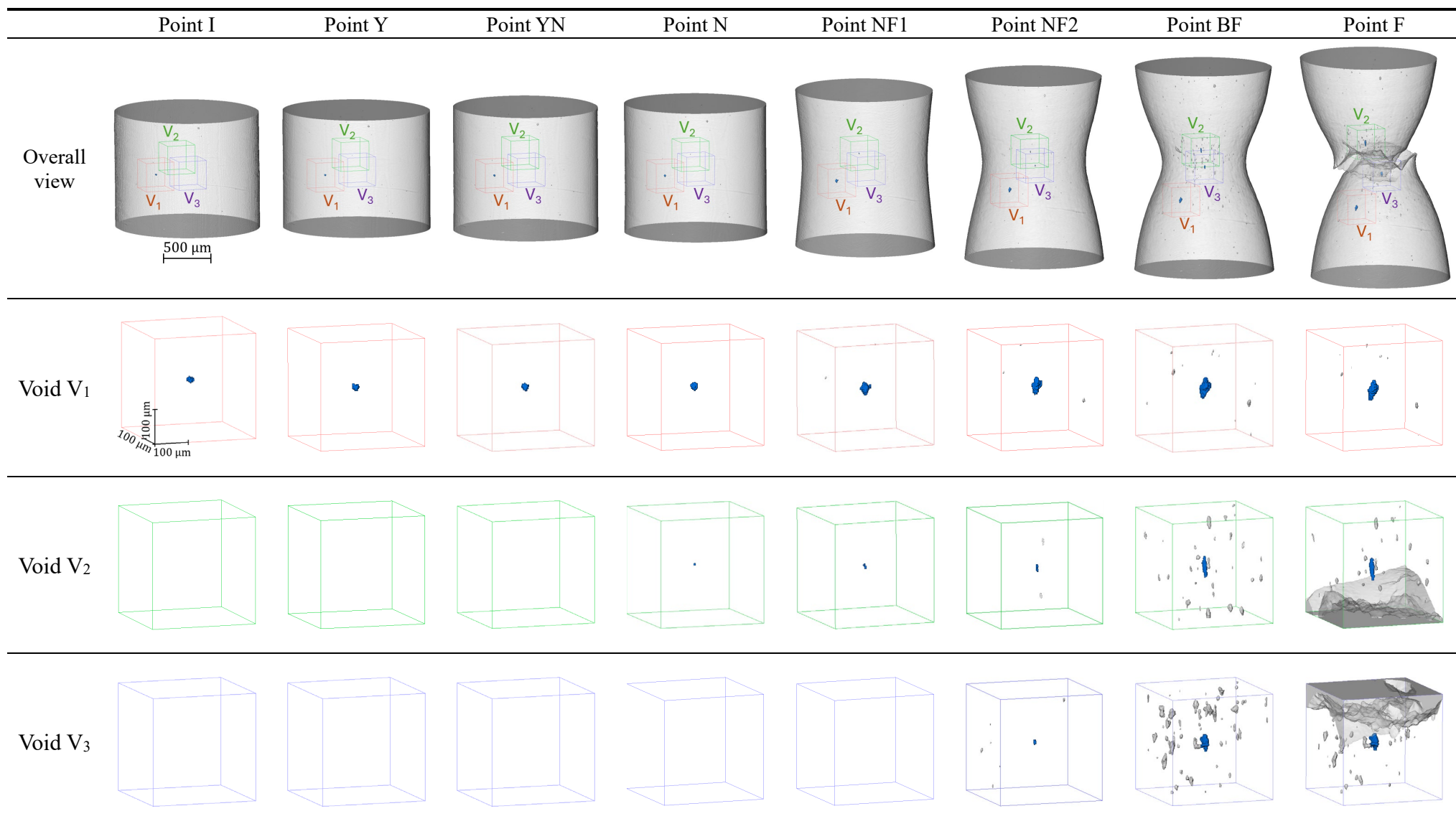
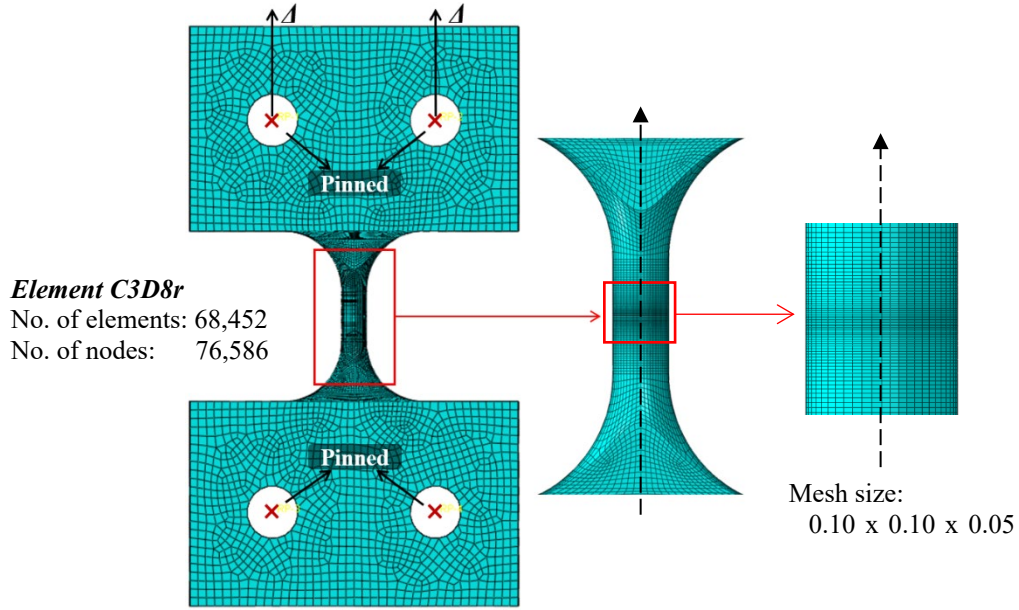


Figure 4.13 3D rendering of micro-void evolution at various deformation points. Coupon S690.

## 4.3 Numerical investigation

### 4.3.1 Establishment of numerical model

Numerical models were established in ABAQUS 2020 for the CT coupons using both measured dimensions and mechanical properties. As shown in [Figure 4.14](#), the coupons were modelled with eight-noded linear hexahedral solid elements, i.e. Elements C3D8R, and reduced integration was adopted to achieve a good balance between numerical accuracy and computational efficiency. In general, the element sizes range typically from 0.2 to 0.5 mm while a fine mesh size with element sizes ranging from 0.05 to 0.1 mm was adopted to model the central parts of the coupons to capture highly localized deformation characteristic. Previous studies [62, 79, 80] have confirmed that these mesh configurations are able to produce converged and accurate results. It is also shown in [Figure 4.14](#) that the model was supported with two pinned locations at its bottom, i.e. to simulate the clamping conditions in the tests of these coupons, while a longitudinal displacement  $\Delta$  is imposed onto its two holes at its top, i.e. to simulate the loading conditions in the tests. It should be noted that these two top holes are assigned to have the same displacements through constraint equations.



**Figure 4.14** Typical finite element model of CT coupons. Model S690.

#### 4.3.2 True stress-strain curves

In order to predict large deformations in the coupons accurately, it is essential to establish a true stress-strain curve for each of the steel. Based on key mechanical properties obtained from standard tensile tests, engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curves are obtained, and they are transformed into the true stress-strain ( $\sigma_t - \epsilon_t$ ) curve through the use of the widely adopted transformation formulation as follows:

$$\sigma_t = \sigma_e(1 + \epsilon_e) \quad (4-3)$$

$$\epsilon_t = \ln(1 + \epsilon_e) \quad (4-4)$$

These formulae are accurate up to necking, i.e. about 5% deformations. After necking, non-uniform deformations occur across the cross-sections of the coupons, and both the true stress  $\sigma_t$  and the true strain  $\epsilon_t$  should be derived from instantaneous dimensions of the coupon. The modified Bridgman method [81] proposed by authors is adopted to revise the

true stress  $\sigma_t$  through the following formula:

$$\sigma_t = \sigma_{av} \cdot \eta_\sigma \quad (4-5)$$

$$\eta_\sigma = \frac{1}{(1 + \frac{2R}{r}) \cdot \ln(1 + \frac{r}{2R})} \quad (4-6)$$

$$\frac{r}{R} = (c_0 + c_{10}\varepsilon_n)^2 \cdot \sum_{k=1}^3 C_k (\varepsilon_{av} - \varepsilon_n)^k \quad (4-7)$$

where

$\eta_\sigma$  is the stress correction factor given in the Bridgman's method as shown in Eq.

6;

$\sigma_{av}$  and  $\varepsilon_{av}$  are the average stress and the average strain respectively,

$$\sigma_{av} = \frac{P}{\pi r_i^2} \quad (4-8)$$

$$\varepsilon_{av} = 2 \ln\left(\frac{r_0}{r_i}\right) \quad (4-9)$$

where

$P$  is the load applied to the coupon,

$r_i$  is the instantaneous radius of the minimum cross-section of the coupon, and

$r_0$  is the initial radius of the coupon.

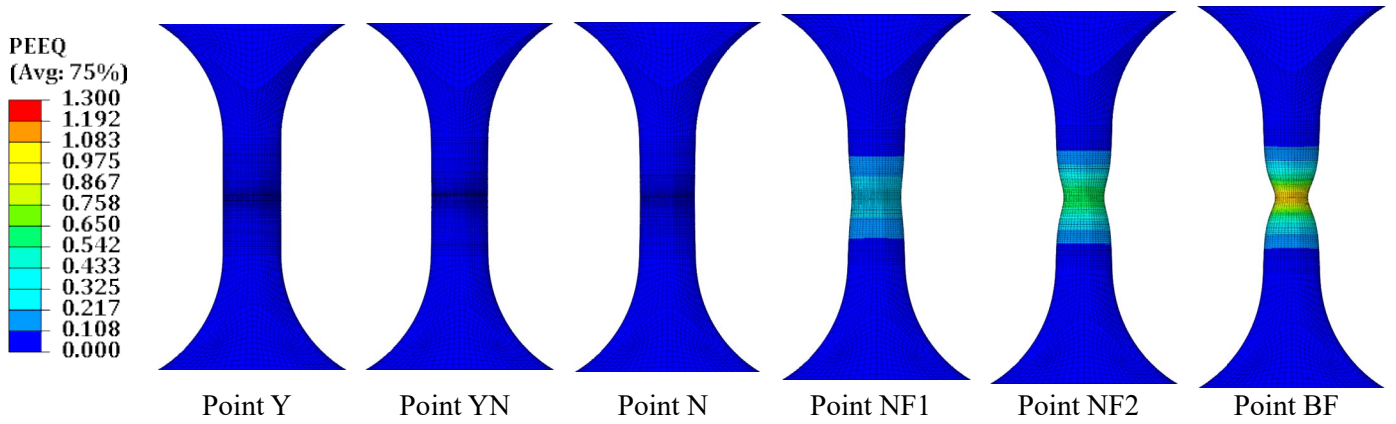
$r/R$  is the spatial necking radii ratio given in the modified Bridgman method as shown in Eq. 4-7, and

$$C_1 = 0.3254, C_2 = 0.6538, C_3 = -0.2677, c_0 = 1.139, \text{ and } c_{10} = -1.219.$$

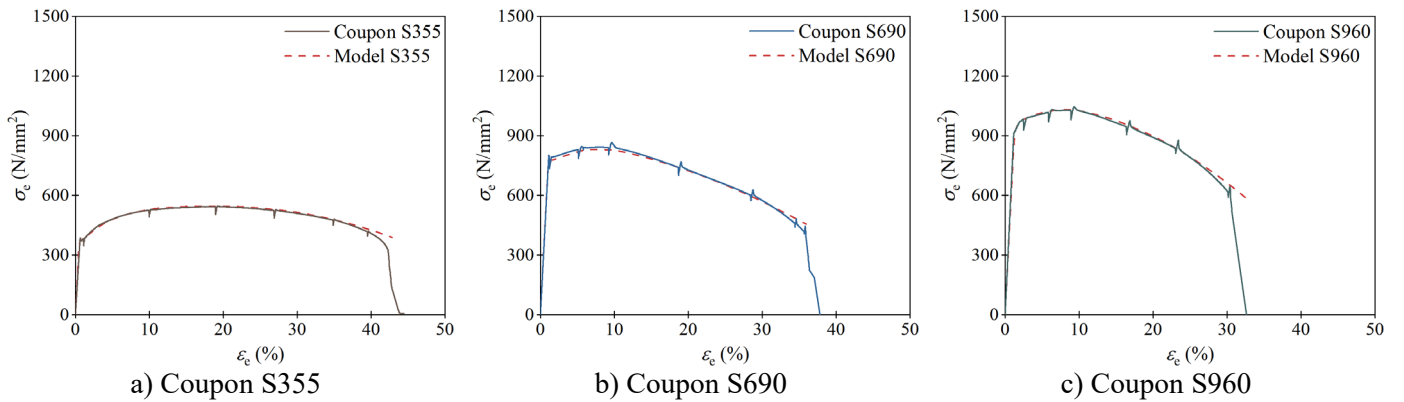
The true stress-strain ( $\sigma_t - \varepsilon_t$ ) curve is corrected and plotted in Figure 4.3b) based on various material properties obtained from the standard tensile tests.

### 4.3.3 Numerical results

Typical deformed meshes at various deformation points of the CT coupons are illustrated in **Figure 4.15** together with the distributions of the equivalent plastic strains PEEQ. It is apparent that PEEQ of those elements in the vicinity of the necking regions of the coupons increase readily at various deformation points. **Figure 4.16** plots the predicted and the measured load-extension ( $N-\Delta$ ) curves of the three coupons. It is apparent that a good agreement between the predicted and the measured results in both the linear and the non-linear deformation ranges is attained.



**Figure 4.15** Deformed meshes of Model S690.



**Figure 4.16** Engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curves of various CT coupons.

Figure 4.17 plots the variations of both the instantaneous radii  $r_i$  and the instantaneous cross-sectional areas  $A_i$  at the critical cross-sections of the CT coupons at various deformation points predicted from the numerical models. It should be noted that after 3D scanning and imaging, the software *Avizo* is able to provide directly the values of both  $r_i$  and  $A_i$  of the CT coupons after 3D reconstruction. Hence, these data are also plotted onto Figure 4.17 for direct comparison with the numerical values. It is apparent that a good agreement between the predicted and the measured data is obtained for both  $r_i$  and  $A_i$  of the CT coupons throughout the entire deformation ranges.

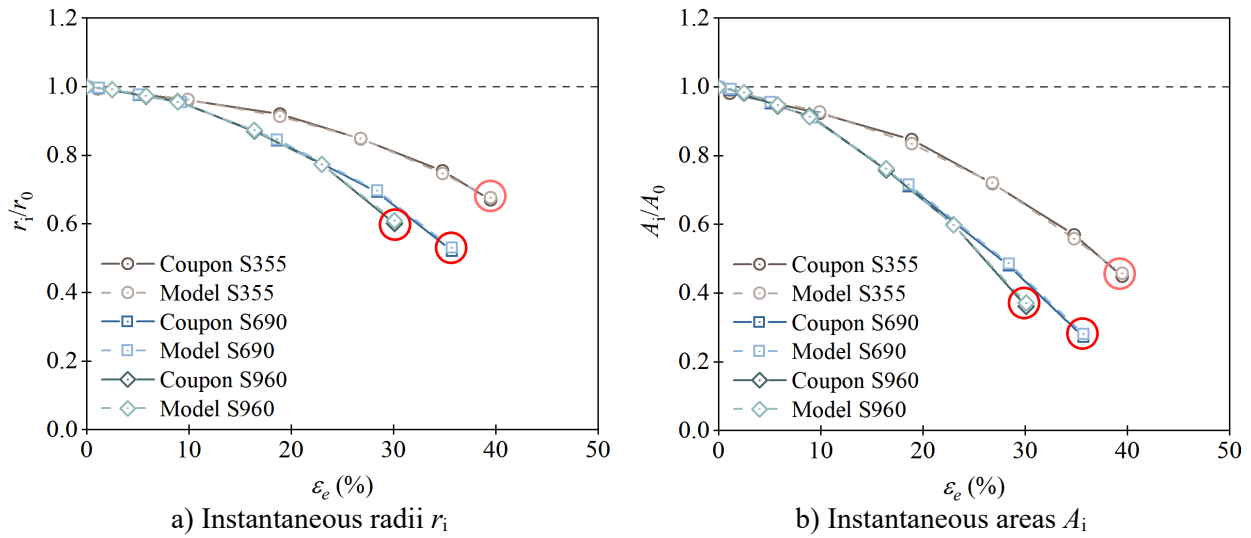


Figure 4.17 Variation of instantaneous dimensions against engineering strain  $\epsilon_e$ .

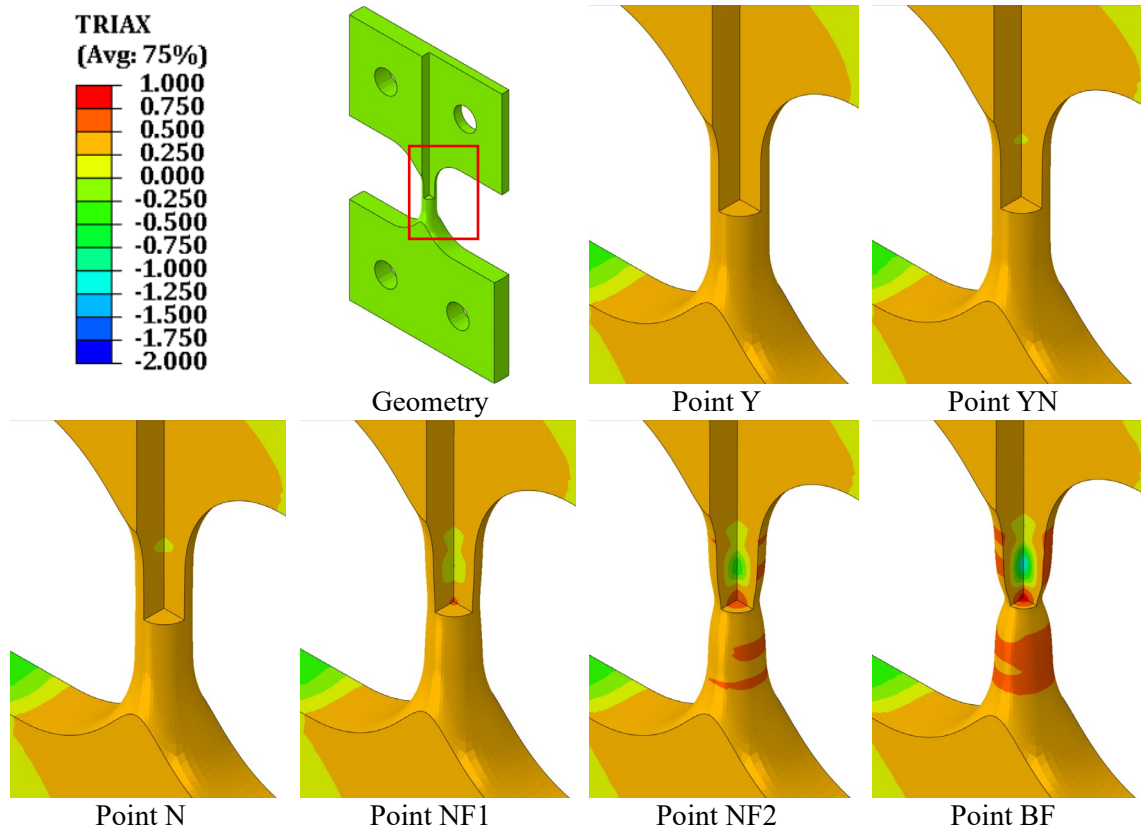
#### 4.3.3.1 Distributions of stress triaxiality

In order to examine the stress states of the CT coupons, the stress triaxiality of the central parts of the coupons at various deformation points are plotted in Figure 4.18, together with the deformations of the necking region. It is shown that

- Before the point of necking, i.e. Point N, the distribution of stress triaxiality within the central part of the coupon remains to be uniform.

- After necking, i.e. Points NF1 and NF2, large deformations take place within the central part of the coupon at which the value of stress triaxiality approaches 0.6.
- At the point of fracture initiation, i.e. Point BF, the steel outside the necking region experiences a spring-back along the longitudinal direction because of unloading, and the stress triaxiality is reduced. Hence, a region with a low stress-triaxiality is formed in the vicinity of the critical cross-section.

Combining the information presented in both [Figure 4.8](#) and [Figure 4.18](#), it is noted that the voids always nucleate and grow in the regions of high stress triaxiality while voids in those regions of low stress triaxiality are hard to develop.



[Figure 4.18](#) Distributions of stress triaxiality at various deformation points. Coupon S690.

#### 4.3.3.2 Verification of Rice-Tracey formula

The void growth theory proposed by Rice and Tracey [18] are widely accepted over the

past few decades, and their proposed formula has been used in many fracture models in the literature. However, the Rice-Tracey formula was developed when experimental techniques were not sufficiently advanced to provide a direct validation, resulting in a lack of experimental evidence to support their theoretical solutions for many years. The Rice-Tracey formula describes the relationship between the void growth rate and the stress triaxiality as follows:

$$\frac{dr}{r_i} = c \cdot \exp(1.5 \cdot T) \cdot d\varepsilon_{eq} \quad (4-10)$$

where  $r_i$  is the instantaneous radius of a void,  $r_0$  is the initial radius of a void, and  $c$  is an independent parameter. By integrating Eq. 4-10, the degree of void growth, or the parameter for void evolution, during a loading process is formulated as follows:

$$\ln\left(\frac{r_i}{r_0}\right) = c \cdot \int_0^{\varepsilon_{eq}} \exp(1.5 \cdot T) \cdot d\varepsilon_{eq} \quad (4-11)$$

Based on the measured results, most of voids are not perfect spheres. Therefore, the void volume is introduced to replace the void radius in the formula, and the Rice-Tracey formula is expressed as follows:

$$\ln\left(\frac{V_i}{V_0}\right) = c \cdot \int_0^{\varepsilon_{eq}} \exp(1.5 \cdot T) \cdot d\varepsilon_{eq} \quad (4-12)$$

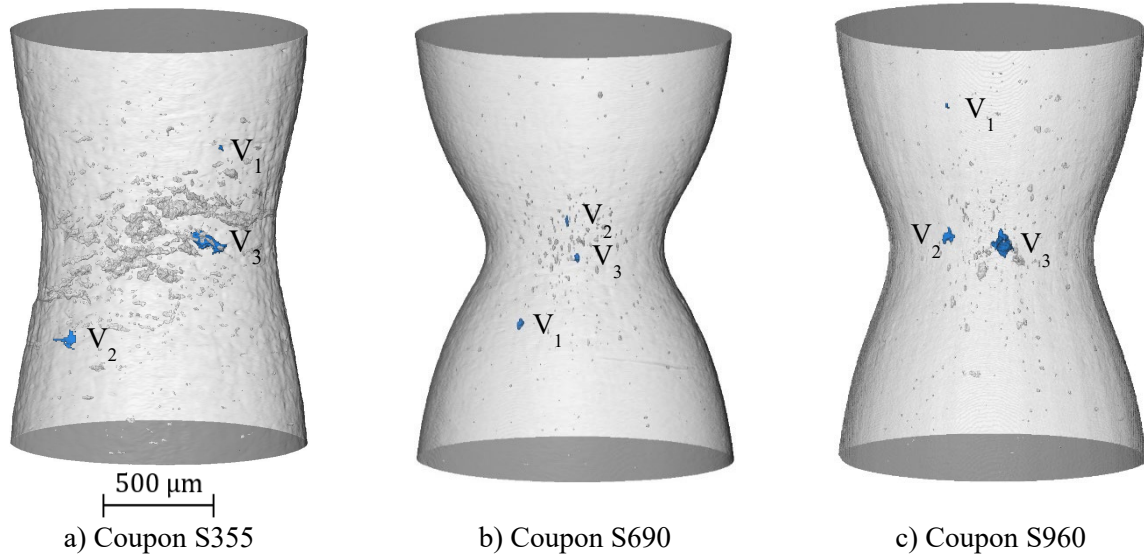
where  $V_i$  is the instantaneous volume size of a void, and  $V_0$  is the initial void volume. The left-hand side of Eq. 4-12 quantifies the enlargement of micro-voids during the loading process while the right-hand side represents the accumulated effects of the states of ‘stress and strain’. Both sides of Eq. 4-12 are readily re-presented with symbols as follows to facilitate further analysis:



$$\gamma_V = \ln \left( \frac{V_i}{V_0} \right)$$

$$\gamma_s = \int_0^{\varepsilon_{eq}} \exp (1.5 \cdot T) \cdot d\varepsilon_{eq}$$

For ease of discussion, three representative voids, namely, Voids V<sub>1</sub>, V<sub>2</sub> and V<sub>3</sub>, of the three CT coupons are selected to evaluate the relationship between the micro-void evolution and the stress triaxiality  $T$  together with the equivalent plastic strain  $\varepsilon_{eq}$ . It should be noted that Void V<sub>1</sub> is the largest pre-existing micro-void while V<sub>2</sub> and V<sub>3</sub> are newly born voids formed during the loading process. Both the shapes and the locations of these micro-voids in deformation point BF are shown in **Figure 4.19**, and their sizes and their states of ‘stress and strain’ are summarized in **Table 4-5**.



**Figure 4.19** Selected micro-voids at Point BF.

The relationship between the void evolution and the equivalent plastic strain of the three CT coupons is shown in **Figure 4.20**. It is evident that the sizes of the newly born voids, i.e. Voids V<sub>2</sub> and V<sub>3</sub>, increase steadily as the equivalent plastic strain increases. To further

quantify the observed evolution behaviour of these micro-voids, the Rice-Tracey formula, i.e. Eq. 12 is applied. Then the values on both sides of Eq. 4-12, i.e.  $\gamma_V$  and  $\gamma_s$ , are calculated respectively based on the results and these values are plotted in Figure 4.21. It is noted that, the parameter  $\gamma_V$  shows a linear relationship with the parameter  $\gamma_s$ . After calibration, parameter  $c$  is found to be 4.441, 1.096 and 2.668 for Coupons S355, S690 and S960, respectively. Hence, the Rice-Tracey formula is demonstrated to be able to describe the micro-voids evolution of these coupons effectively throughout the entire tensile tests.

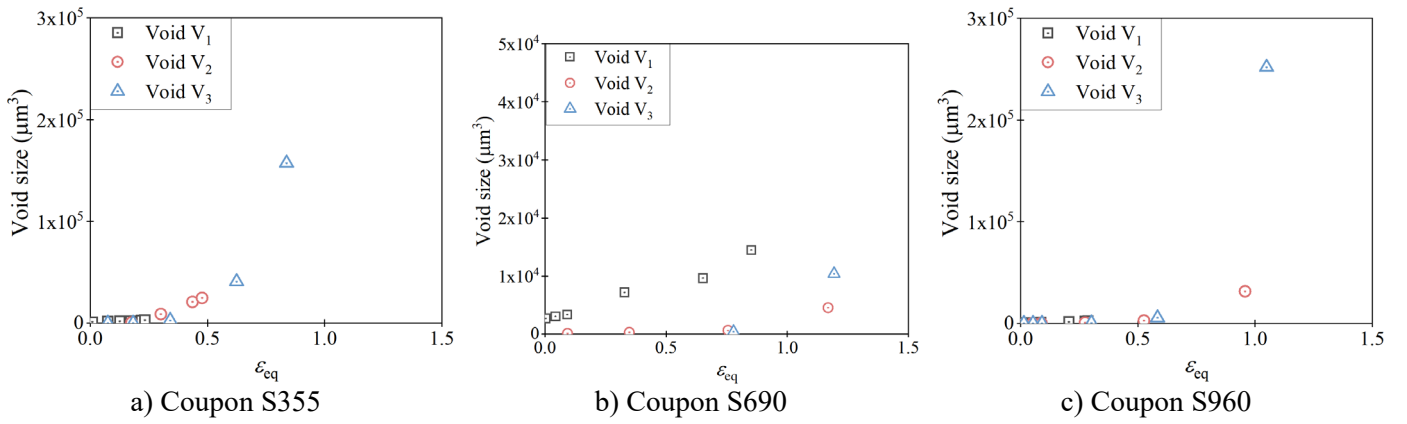
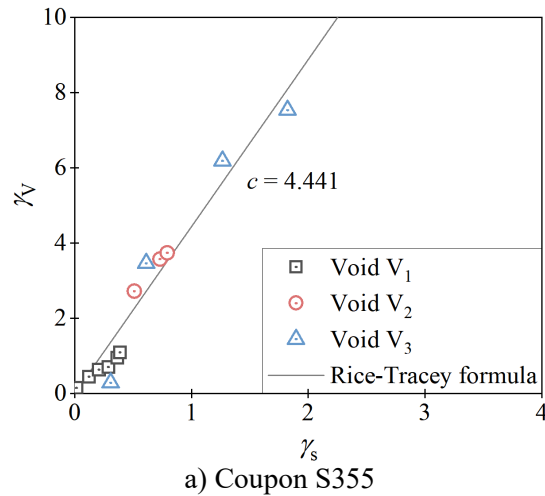
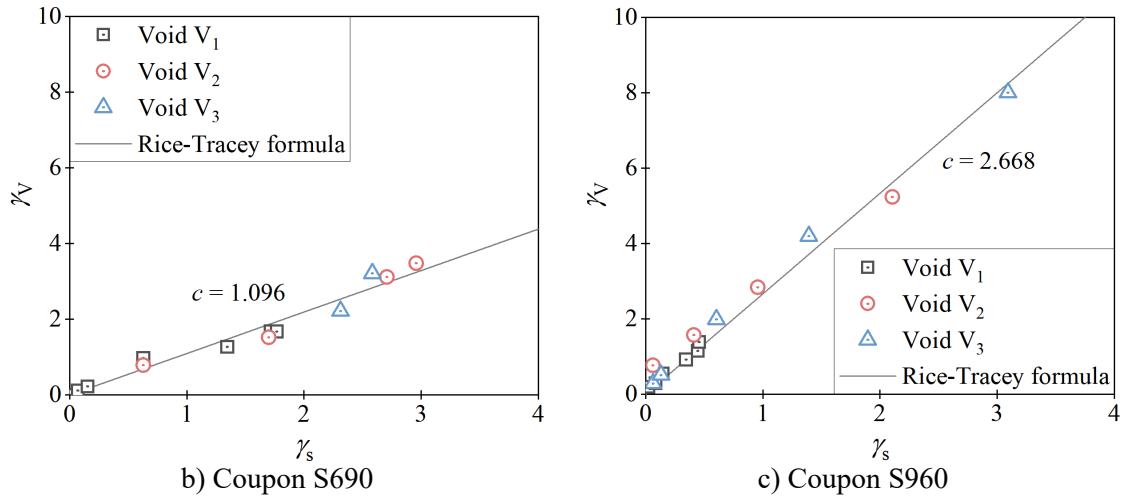


Figure 4.20 Evolution of void with the change of equivalent plastic strain  $\epsilon_{eq}$ .





**Figure 4.21** Comparison between Rice-Tracey formula and experimental results.

#### 4.3.4 Void-related parameters in GTN model

Key volume fraction derived from the *in-situ* X-ray CT quantification is summarized in this section, providing necessary inputs for calibrating the Gurson-Tvergaard-Needleman (GTN) model. The parameters focus on four important stages of void development: initial volume fraction ( $f_0$ ), nucleated volume fraction ( $f_N$ ), critical volume fraction ( $f_c$ ) linked to accelerated void coalescence, and failure volume fraction ( $f_f$ ) at fracture initiation. The initial volume fraction corresponds to the value at Point I, and the failure volume fraction corresponds to the value at Point BF. It should be noted that, since the new micro-voids nucleate at every stage of the loading process, as shown in [Figure 4.8](#), the value of  $f_N$  is suggested to be set as the volume fraction at yielding state (Point Y). The phenomenon of void coalescence is observed in the stage of rapid growth of void after necking. Therefore, the critical volume fraction is defined as the volume fraction at Point N. Experimental values of these four parameters for the three steel grades are listed in [Table 4-5](#). Compared to the values listed in [Table 2-3](#), there are significant differences between the experimental values and the calibrated values of  $f_0$ ,  $f_N$ ,  $f_c$  and  $f_f$ . This may be due to various assumptions

and simplifications involved during the calibration process of the GTN model.

**Table 4-5** Measured values of parameters GTN model.

| Steel grade | Initial void<br>fraction<br>$f_0$ (%) | Void nucleation<br>fraction<br>$f_N$ (%) | Critical void<br>fraction<br>$f_c$ (%) | Failure void<br>fraction<br>$f_f$ (%) |
|-------------|---------------------------------------|------------------------------------------|----------------------------------------|---------------------------------------|
| S355        | 0.0001                                | 0.0001                                   | 0.0005                                 | 0.1526                                |
| S690        | 0.0004                                | 0.0005                                   | 0.0006                                 | 0.0127                                |
| S960        | 0.0003                                | 0.0005                                   | 0.0009                                 | 0.0276                                |

## 4.4 Conclusions

This study investigated the micro-void evolution in structural steel through the use of in-situ X-ray computed tomography (CT), and specially designed steel coupons were loaded in displacement control to various elongations, and scanned simultaneously to capture formation or growth of micro-voids with a minimum volume of  $56 \mu\text{m}^3$  up to fracture. Three CT coupons of different steel grades, i.e. S355, S690 and S960 steel, were investigated to track key stages of micro-void evolution, i.e. nucleation, growth, and coalescence, through scanning and imaging at critical deformation points. High-resolution 3D reconstruction technique was employed to conduct detailed morphological analysis of the micro-voids captured, and then, their sizes, locations and distributions in the vicinity of the necking region of the coupons were examined. Numerical models were then established to examine the influence of stress triaxiality on the development of micro-voids.

Key findings of the study are summarized as follows:

- Through the use of the in-situ X-ray computed tomography, both pre-existed and newly born voids within the regions of interest of three CT coupons have been detected successfully. These data form the basis of a scientific investigation into the micro-void evolution of structural steel.

- In general, the majority of the micro-voids are found to be formed in the vicinity of the minimum cross-sections of the coupons. The numbers of voids at various deformation points are marked in the engineering stress-strain curves of the coupons to provide a direct comparison on the process of micro-void evolution within the coupons with that of ‘macro-elongation’ of the coupons.
- A remarkable micro-void evolution is evident after the onset of necking in all the coupons. It is established that Coupon S355 exhibits larger value of numbers and sized of micro-voids than those of Coupons S690 and S960, and a substantially larger void volume fraction (0.1526%) is found in Coupon S355 at the deformation point close to fraction initiation, when compared with those found in Coupons S690 and S960 at fracture initiation.
- As a whole, the void volume fractions of Coupons S355, S690 and S960 at the point of initiation of fracture are shown to be rather limited, i.e.  $\leq 0.2\%$ , and hence, it is difficult to relate these directly to fracture in the coupons, especially for Coupons S690 and S960.
- Based on the numerical results of carefully established finite element models, the micro-void growth is demonstrated to be highly dependent on both the stress triaxiality  $T$  and equivalent plastic strain  $\varepsilon_{eq}$ . After investigating the measured void sizes and the predicted stress states obtained from the numerical models, the Rice-Tracey formula is verified to be able to describe the micro-voids evolution of these coupons effectively throughout the entire tensile tests.

- The measured void volume fractions allow an effective calibration on various void-related parameters of the classical GTN model to capture the ductile fracture behaviour of structural steel under loading.

The integration of CT scanning and imaging, mechanical testing, and numerical modelling highlighted the importance on the role of stress states in micro-void evolution, particularly during necking and fracture initiation. An attempt is made to relate various post-necking deformation stages of the coupons up to fracture initiation with various stages of micro-void evolution, with special emphasis on the presence of micro-voids at the minimum cross-sectional areas of the coupons near fracture. This work elaborates the interaction between structural behaviour of the coupons and their micro-void evolution, providing a reference for prediction on ductile fracture.

## Chapter 5

### Ductile fracture behavior of S690 high strength steel under combined tension and shear actions

#### 5.1 Introduction

In this chapter, the effects of combined tension and shear actions on the ductile fracture behaviour of S690 high strength steel were investigated through experimental and numerical approaches. Both the stress triaxiality  $T$  and the Lode parameter  $L$  are introduced to evaluate effects of stress concentration and deviatoric stresses separately. Five types of notched specimens with different levels of stress triaxialities and Lode parameters were designed to decouple the influence of stress triaxiality and Lode parameters. Tensile tests on a total of 34 notched specimens were conducted carefully to capture their deformations up to fracture. Visual examination on fractured specimens using scanning electron microscopy (SEM) was also conducted to identify microstructural differences between tension- and shear-dominated fracture.

Advanced numerical modelling using three dimensional solid elements was then carried out to simulate structural behaviour of these notched specimens up to fracture, and critical states of both stresses and strains for each of these coupons at fracture were identified. These parameters were employed to calibrate a total of five different modern fracture models, namely, the Xue-Wierzbicki model, the Bai-Wierzbicki model, the modified Mohr-Coulomb model, the Lou-Huh model, and the Lode angle Modified Void Growth model. Their capabilities in capturing the fracture behaviour of the S690 steel under i) tension

actions, ii) shear actions, and iii) combined tension and shear actions were also examined. Based on the numerical models and a wide range of the Lode parameters obtained above, a new ductile fracture model is proposed for the S690 high strength steel under combined tension and shear actions.

## 5.2 Monotonic tests on specimens of S690 steel

### 5.2.1 Design of notched specimens

For those steel members fractured under tension or shear actions in practice, the stress triaxiality  $T$  and the Lode parameter  $L$  at the critical positions of these members were found to range typically from 0 to 1.5 and -1 to 0, respectively. In order to generate test data under a wide range of practical stress states, a number of test specimens with notches of different shapes and dimensions were designed, and a comprehensive test programme, as shown in Table 5-1, is devised. The design of the test programme is described as follows:

- 1) ***Cylindrical standard coupons***: Cylindrical standard coupons (CSC) are used to calibrate the constitutive relationship of the steel, which configuration is shown in Figure 5.1a). In addition, they provide demarcation points for various regions of low, medium, and high triaxiality, as shown in Figure 5.2.
- 2) ***Cylindrical notched coupons***: Being similar to those standard coupons, these coupons are notched along their perimeters at their mid-length, as shown in Figure 5.1b). Hence, stresses are concentrated at the centre of the minimum cross-section, and the stress triaxiality increases with a decrease in the notch radius. Three notch radii are adopted: 5.0, 2.5 and 1.25 mm for Cylindrical notched coupons CNC-R5, CNC-R2.5 and CNC-R1.25, respectively. These coupons provide data for high triaxiality and high Lode

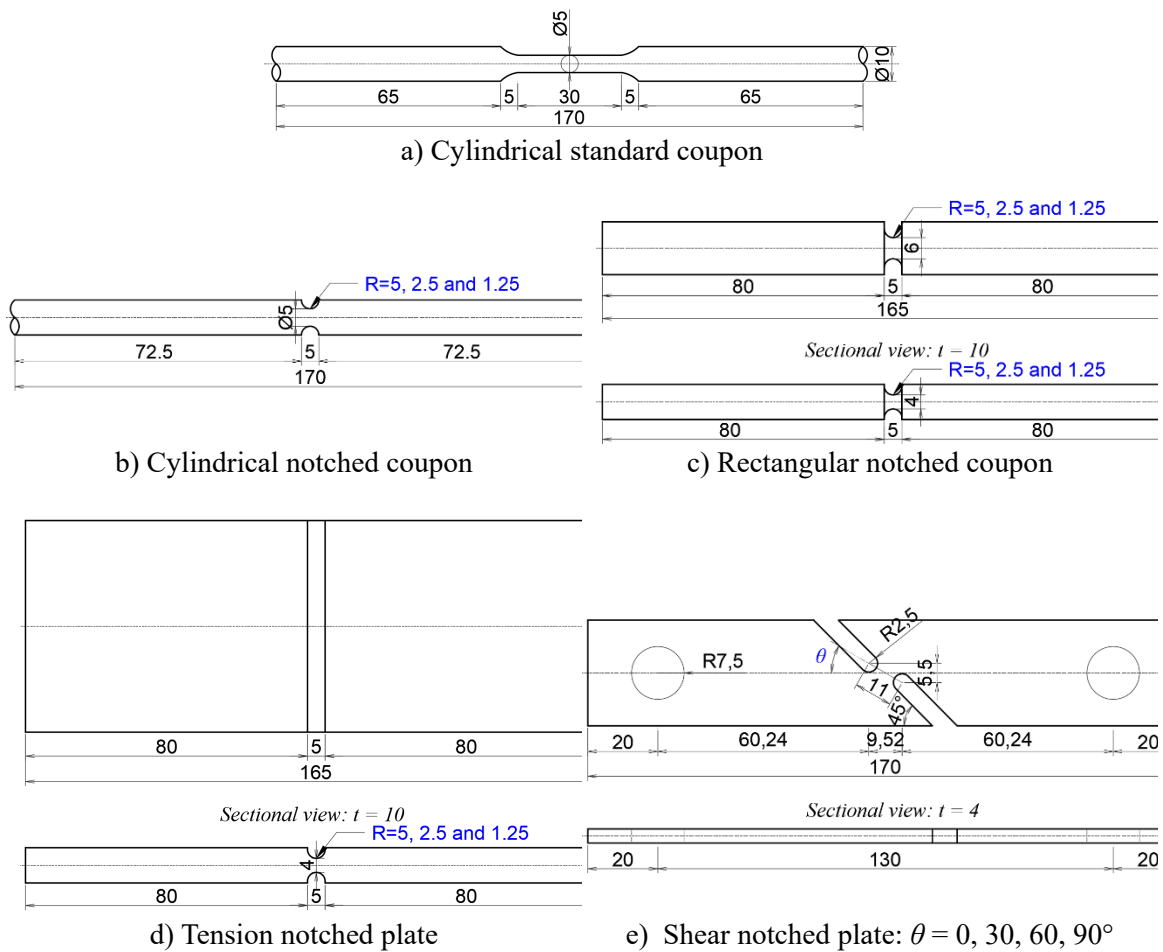


parameters.

- 3) ***Rectangular notched coupons***: Being similar to those cylindrical notched coupons, these coupons are notched as cuboids with rectangular cross-sections at their mid-lengths, as shown in [Figure 5.1c](#)). Three notch radii are adopted: 5.0, 2.5 and 1.25 mm for rectangular notched coupons RNC-R5, RNC-R2.5 and RNC-R1.25, respectively. These coupons provide data for high triaxiality but medium Lode parameters.
- 4) ***Tension notched plates***: Steel plates are cut out, and grooves are machined on both surfaces of the plates at mid-length along their widths, as shown in [Figure 5.1d](#)). The groove radii are 5.0, 2.5 and 1.25 mm for Tension notched plates TNP-R5, TNP-R2.5 and TNP-R1.25 respectively. Since the widths of the plates are ten times larger than their thicknesses, the tension notched plates are under a plane strain state, and hence, they provide data for high triaxiality but with Lode parameters at zeros.
- 5) ***Shear notched plates***: Steel plates are cut out, and narrow notches are machined into specific “butterfly” patterns designed by Bao and Wierzbicki [23]. It should be noted that under tensile tests, the orientation of the central line of the notched parts imposes various combinations of tension and shear actions onto the notches, as shown in [Figure 5.1e](#)). The angles between the central lines of the notched parts of the steel plates and the loading direction are set at 0°, 30°, 60° and 90° for shear notched plates SNP-00, SNP-30, SNP-60 and SNP-90, respectively, and they provide data for low to medium triaxialities. It should be noted that these shear notched plates are *key specimens* to be investigated in the present study.

**Table 5-1** Test programme.

| <b>Actions</b>        | <b>Specimens</b>                            | <b>Parameters</b>                                                                                                       | <b>No. of tests</b> |
|-----------------------|---------------------------------------------|-------------------------------------------------------------------------------------------------------------------------|---------------------|
| <b>Tension action</b> | Cylindrical standard coupons<br>Coupons CSC | 2 No.                                                                                                                   | 2                   |
|                       | Cylindrical notched coupons<br>Coupons CNC  | 3 No. @ $r = 1.25$ mm<br>3 No. @ $r = 2.5$ mm<br>3 No. @ $r = 5.0$ mm                                                   | 9                   |
|                       | Rectangular notched coupons<br>Coupons RNC  | 3 No. @ $r = 1.25$ mm<br>3 No. @ $r = 2.5$ mm<br>3 No. @ $r = 5.0$ mm                                                   | 9                   |
|                       | Tension notched plates<br>Coupons TNP       | 2 No. @ $r = 1.25$ mm<br>2 No. @ $r = 2.5$ mm<br>2 No. @ $r = 5.0$ mm                                                   | 6                   |
|                       | Shear notched plates<br>Coupons SNP         | 2 No. @ $\theta = 0^\circ$<br>2 No. @ $\theta = 30^\circ$<br>2 No. @ $\theta = 60^\circ$<br>2 No. @ $\theta = 90^\circ$ | 8                   |



**Figure 5.1** Dimensions of S690 specimens (mm).

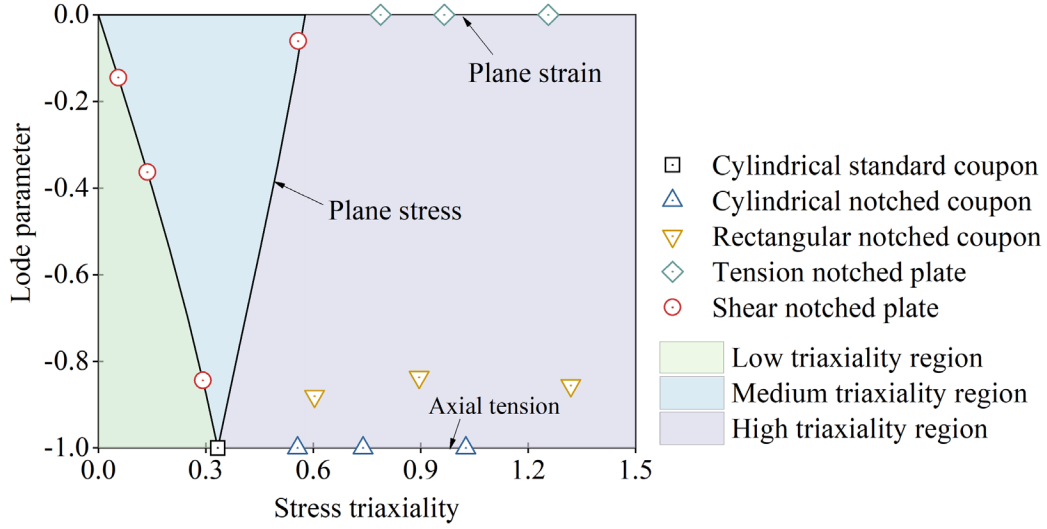


Figure 5.2. Conceptual representation of the initial stress states.

The initial stress states of these specimens are calculated with analytical expressions [82, 83] given in Table 5-2, which are plotted in the stress triaxiality  $T$  – Lode parameter  $L$  space, i.e. the  $T$ – $L$  space, shown in Figure 5.2. It should be noted that, for both the rectangular notched coupons and the shear notched plates, no analytical expressions are available, and hence the initial stress states are extracted from finite element models under small deformations. In general, the  $T$ – $L$  space shown in Figure 5.2 is divided into 3 regions according to various stress states, which is expressed as [83]:

$$\frac{L(L-3)(L+3)}{(L^2+3)^{3/2}} = -\frac{27}{2}T(T^2 - \frac{1}{3}) \quad (5-1)$$

The green region represents the state of low stress triaxiality, and both the states under pure shear stresses, and combined tension and shear stresses fall into this region. The blue region refers to the state of medium stress triaxiality, where the magnitude of stress triaxiality is not large enough to make all principal stresses positive. The purple region represents the state of high stress triaxiality, and includes most situations of tension stress states.

**Table 5-2.** Analytical expressions for initial stress state

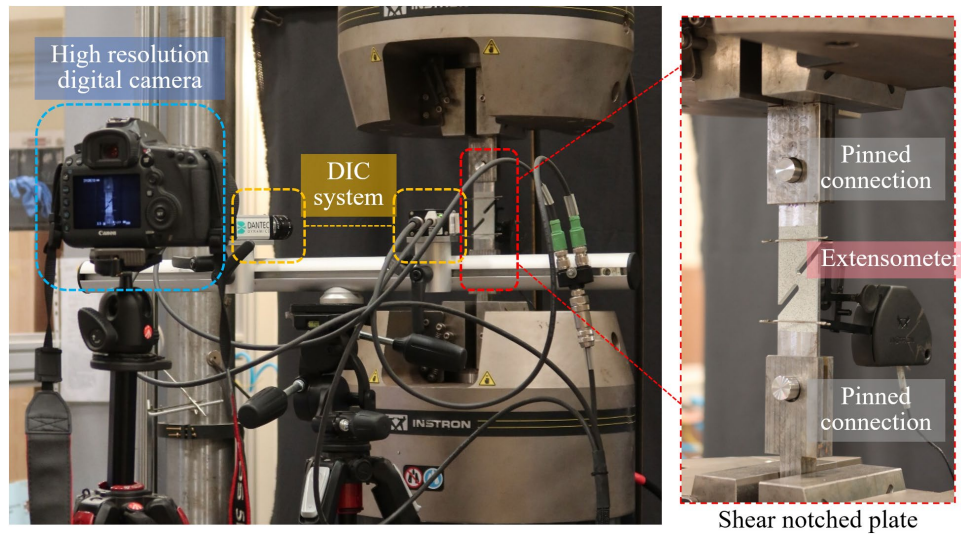
| Specimen type                                           | Stress triaxiality, $T$                                                   | Lode parameter, $L$ |
|---------------------------------------------------------|---------------------------------------------------------------------------|---------------------|
| Cylindrical standard coupon<br>under tension action     | $\frac{1}{3}$                                                             | -1                  |
| Cylindrical notched coupon<br>under tension action [21] | $\frac{1}{3} + \ln \left(1 + \frac{a}{2R}\right)$                         | -1                  |
| Rectangular notched coupon<br>under tension action      | ---                                                                       | ---                 |
| Tension notched plate<br>under tension action [22]      | $\frac{\sqrt{3}}{3} \left[1 + 2 \ln \left(1 + \frac{t}{4R}\right)\right]$ | 0                   |
| Shear notched plate<br>under shear action               | 0                                                                         | 0                   |

### 5.2.2 Experimental investigation

All the specimens were cut from a 10 mm thick S690 steel plate. All the monotonic tensile tests were carried out using a 500 kN multi-functional tension/compression testing system. An extensometer and a high-resolution digital camera were adopted to capture deformations of the specimens during tests. The gauge lengths of the cylindrical standard coupons, the cylindrical notched coupons, the rectangular notched coupons, and also the tension notched plates were 25 mm, while it was 50 mm for the shear notched plates. The test setup is illustrated in [Figure 5.3](#), and all the tests were conducted under displacement control. The loading rate at the elastic stage was set at 0.05 mm/min, and it gradually increased to 0.1 mm/min before the peak load was reached, and then adjusted to 0.2 mm/min until fracture.

It should be noted that for all the shear notched plates, a digital image correlation system (DIC) with two high-definition cameras was employed to measure surface deformations of the specimens, and hence, their strain fields were accurately measured. In order to achieve

an ideal state of shear stresses, all these specimens were loaded through pinned connections at both ends.



**Figure 5.3** Test setup for monotonic tensile tests.  
An Instron 8803 with a capacity of 500 kN.

### 5.2.1 Test results

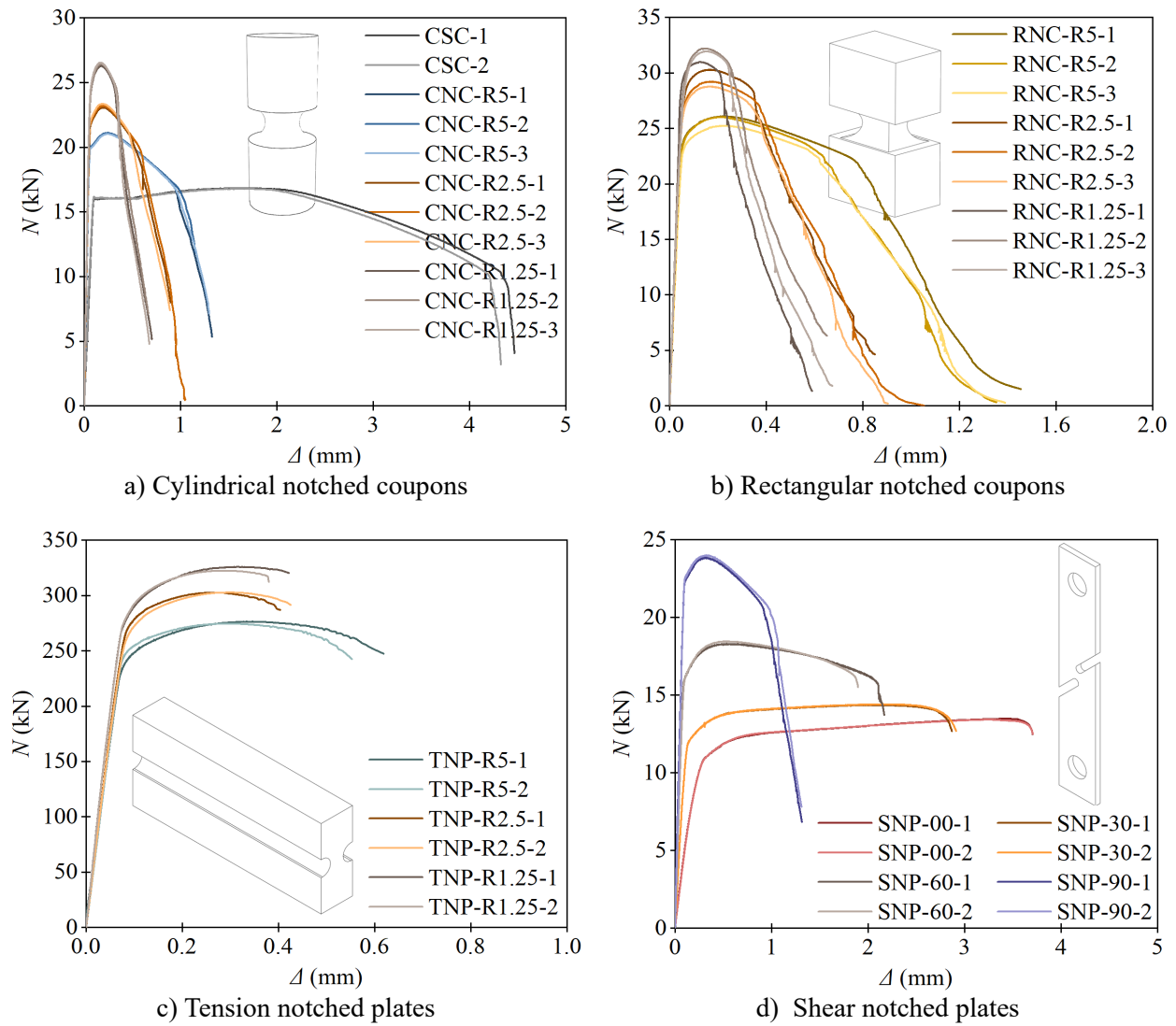
All these tensile tests were conducted successfully, and all the specimens were tested to fracture. Measured load-extension curves of various types of the notched specimens are plotted onto the same graphs in **Figure 5.4** for easy comparison. It should be noted that:

- The cylindrical standard coupon is taken as a reference to provide basic mechanical properties of the S690 steel for all specimens. As shown in **Figure 5.4a**), there is an obvious yielding plateau for the coupon at the end of its elastic deformation stage, and then, there is a gradual increase in the applied load till reaching its maximum value, which is also the point of onset of necking. Then, the coupon exhibits large deformations up to fracture, and the initiation of fracture is identified by a sudden change in the slope of the curve.

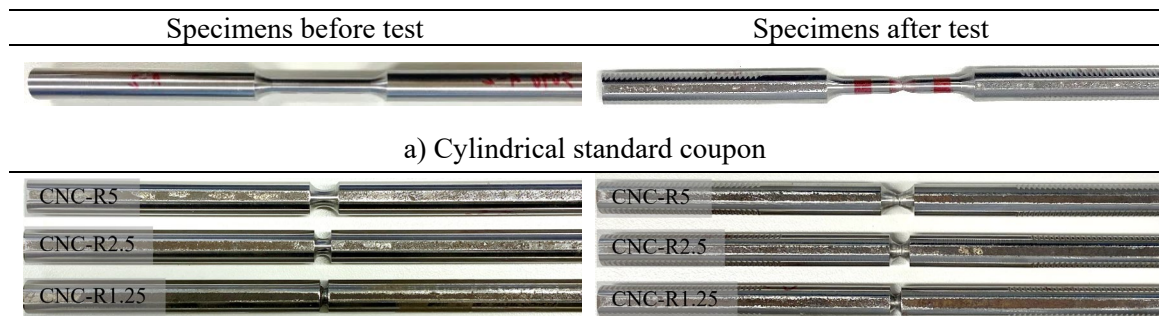
- For cylindrical notched coupons, as also shown in [Figure 5.4a](#)), the maximum applied loads of these coupons are found to increase, and yet, their extension limits are found to be decreased with a decrease in their notch radii. Moreover, the on-set of necking in these coupons is found to take place at smaller extensions, when compared with those in the cylindrical standard coupons.
- Similarly, the maximum applied loads of those rectangular notched coupons are also found to increase, and yet, their extension limits are found to be decreased with a decrease in the dimensions of the notched areas, as shown in [Figure 5.4b](#)). Moreover, the on-set of necking in these coupons is found to take place at smaller extensions when their notch dimensions decrease.
- For those tension notched plates shown in [Figure 5.4c](#)), a decrease in the notched radii always results in an increase in the maximum applied loads of the plates, together with small decreases in their extension limits.
- For those shear notched plates shown in [Figure 5.4d](#)), their deformation characteristics differ significantly when the angle  $\theta$  varies from 0 to 90°. With an increase in the value of  $\theta$ , the maximum applied loads of the plates are found to increase together with significant decreases in their extension limits.

Typical fractured specimens are shown in [Figure 5.5](#) with a comparison with those specimens before tests. All the cylindrical standard coupons, the cylindrical notched coupons, the rectangular notched coupons, and the tension notched plates are found to have fractured at their minimum cross-sections, each with an apparent reduction in their cross-sectional areas in the direction perpendicular to the loading direction. However, the fracture surfaces of the shear notched plates, i.e. Plates SNP-00, SNP-30, SNP-60, and SNP-90

have different particular angles with the loading direction, demonstrating yielding of the steel under various combined tension and shear actions.

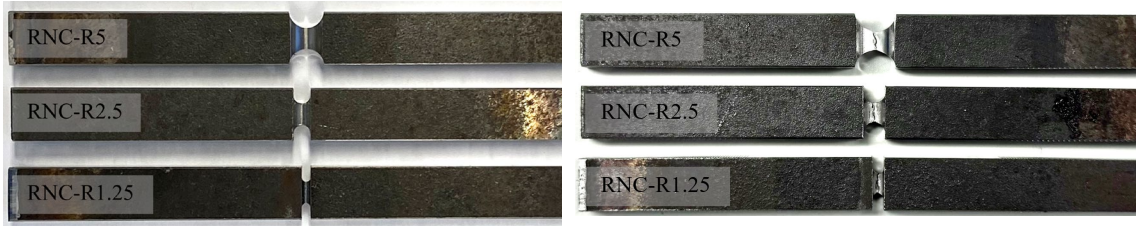


**Figure 5.4** Load-extension curves for notched specimens.





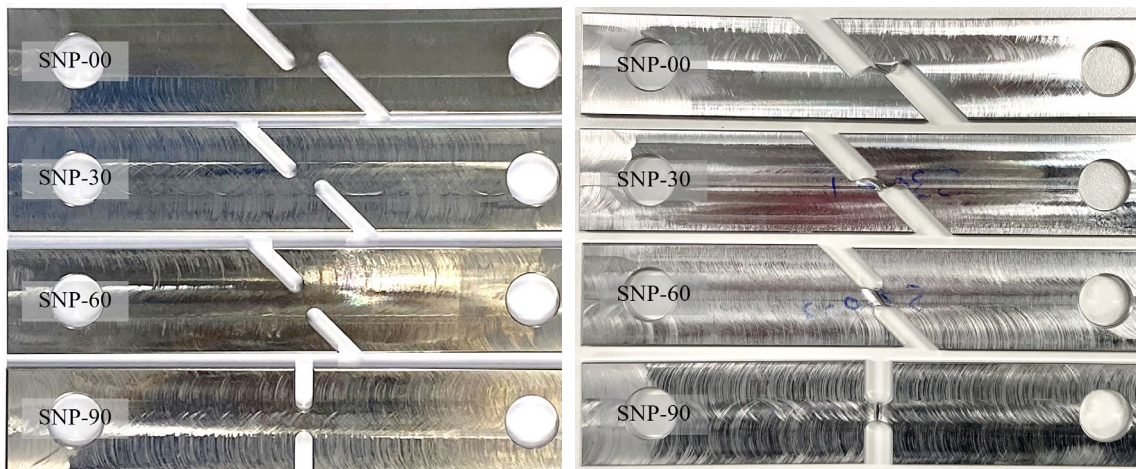
b) Cylindrical notched coupons



c) Rectangular notched coupons



d) Tension notched plates



e) Shear notched plates

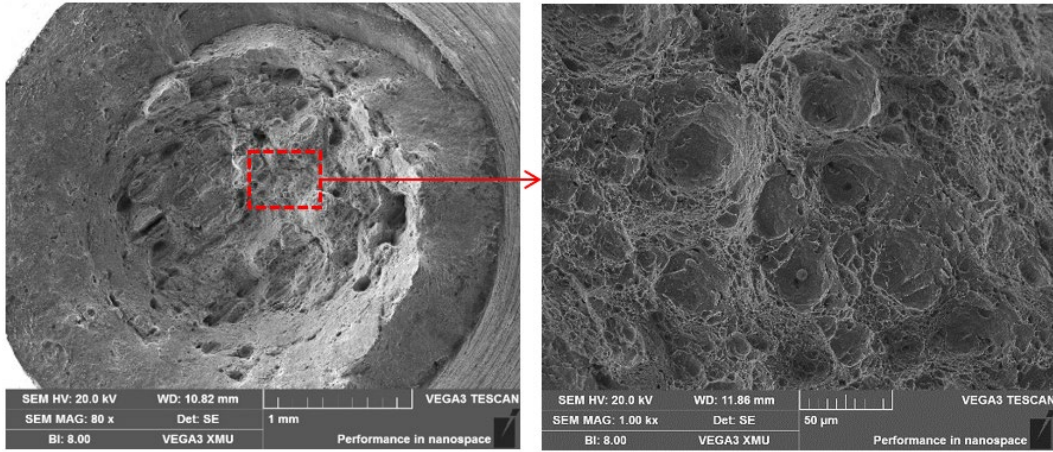
**Figure 5.5** Test specimens before and after tests.



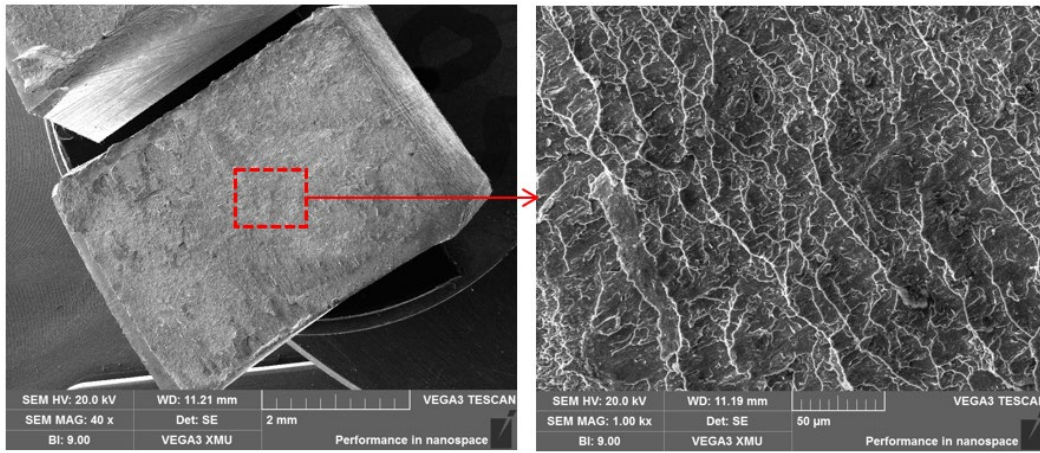
### 3.2.2 Fracture surfaces

Typical fracture surfaces of a cylindrical notched coupon and two shear notched plates are observed under a scanning electron microscope (SEM) to examine their deformed microstructures after tests. These fracture surfaces are shown in **Figure 5.6**, and enlarged views of their central regions, which are their representative parts, are provided. It should be noted that:

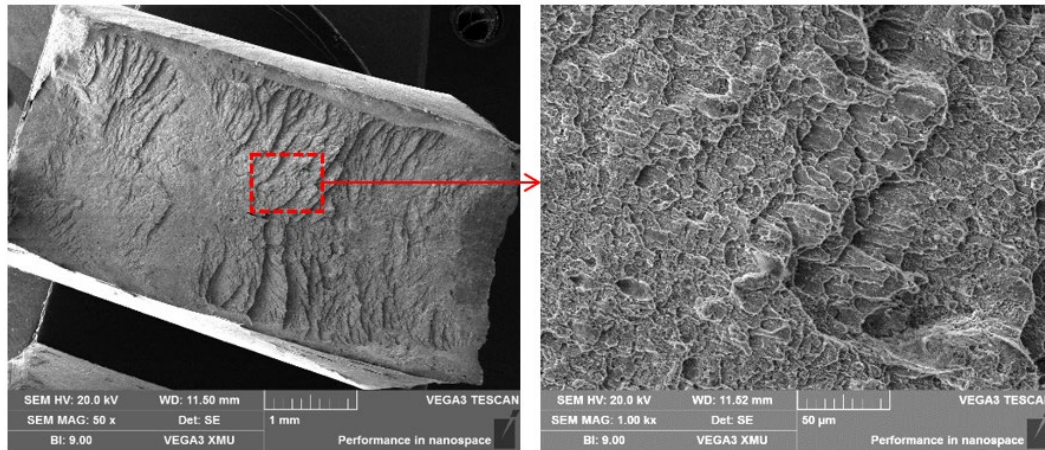
- The fracture surface of the cylindrical notched coupon shown in **Figure 5.6a**) is considered to be a typical microstructure of ductile fracture caused by tensile actions. Dimples are observed with inclusions or micro-cracks across the fracture surface.
- The fracture surface of the shear notched plate shown in **Figure 5.6b**) is considered to be a typical microstructure of ductile fracture caused by shear actions, and no dimples are found across the fracture surface.
- **Figure 5.6c**) illustrates the fracture surface of the shear notched plate SNP-60 which is considered to be a typical microstructure of ductile fracture caused by combined tension and shear actions. There is an apparent layered feature across the fracture surface together with small elongated dimples.



a) Cylindrical notched coupon, CNC-R5



b) Shear notched plate, SNP-00



c) Shear notched plate, SNP-60

**Figure 5.6** Fracture surfaces of tested specimens under SEM.

### 5.3 Numerical simulation

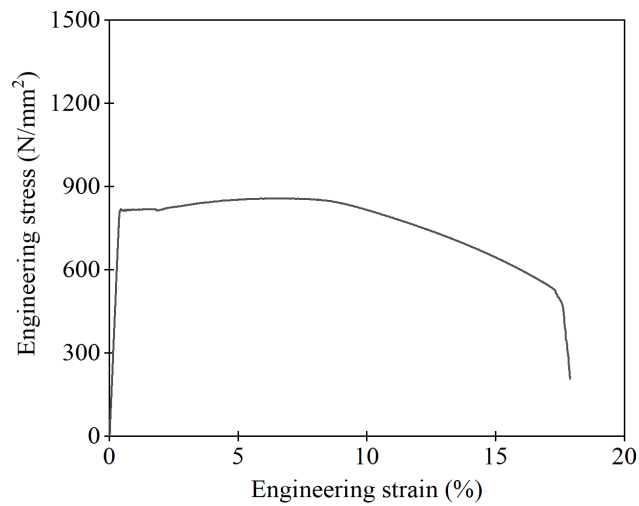
Advanced numerical modelling using three dimensional solid elements was then carried out to simulate structural behaviour of these notched specimens up to fracture, and critical states of both stresses and strains for each of these coupons at fracture were identified. Details of the modelling, such as mesh configurations, boundary conditions and loading applications are presented in this section together with detailed descriptions of the numerical results.

#### 5.3.1 Establishment of numerical models

The constitutive model was obtained from measured stresses and strains of the cylindrical standard coupon test reported in Section 3.2.1, and the engineering stress-strain ( $\sigma_e - \epsilon_e$ ) curve of coupon CSC-1 is plotted in [Figure 5.7](#). Based on the modified Bridgman's method [81] proposed by the authors, the true stress-strain ( $\sigma_t - \epsilon_t$ ) curve was obtained as shown in [Figure 5.8](#). This true stress-strain ( $\sigma_t - \epsilon_t$ ) curve was applied to all models of these notch specimens in the present study.

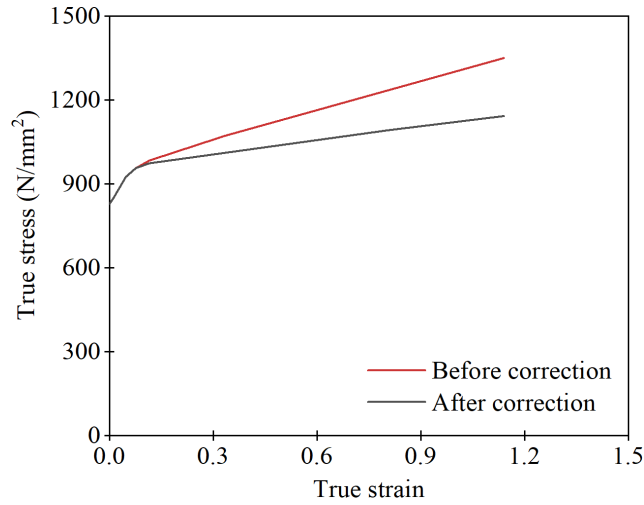
The numerical models were developed in ABAQUS 2020 using the measured geometrical dimensions of the test specimens. As shown in [Figure 5.9](#), the specimens were modelled with eight-node linear hexahedral solid elements, i.e. Element C3D8R, featuring reduced integration to balance computational accuracy and efficiency. As an example, a mesh size of 2.0 to 5.0 mm was implemented globally for the cylindrical standard coupon as shown in [Figure 5.9a](#)), while a mesh size of 0.10 to 0.25 mm was adopted after localized refinement near the mid-lengths of these models. Such a meshing strategy was applied to all the models as shown in [Figure 5.9b\) to e\)](#), and it was considered to be able to resolve strain gradients effectively and maintain computational economy. It should be noted that

these mesh configurations have been confirmed by other researchers [62, 80] as well as the authors that they are able to give reliable and converged numerical results. Different boundary conditions were provided to these models to replicate various conditions imposed on the test specimens in tests. Typically, these models were fully fixed at their bottom ends to simulate clamping conditions while a longitudinal displacement  $\Delta$  was imposed to all the nodes at the top ends of the models which were assigned to have the same displacements through constraint equations. Due to symmetry, only a 1/8 model was adopted for both the cylindrical standard and the cylindrical notched coupons, a 1/2 model was adopted for the rectangular notched coupons, and also for the tension notched plates, while a full model was adopted for the shear notched plates. It should also be noted that a pinned-pinned condition at both ends of each of the shear notched plates was adopted according to the loading condition in tests, and hence, the models were properly aligned to ensure effective load transfer across the specifically designed notched areas with different inclined angles  $\theta$ .

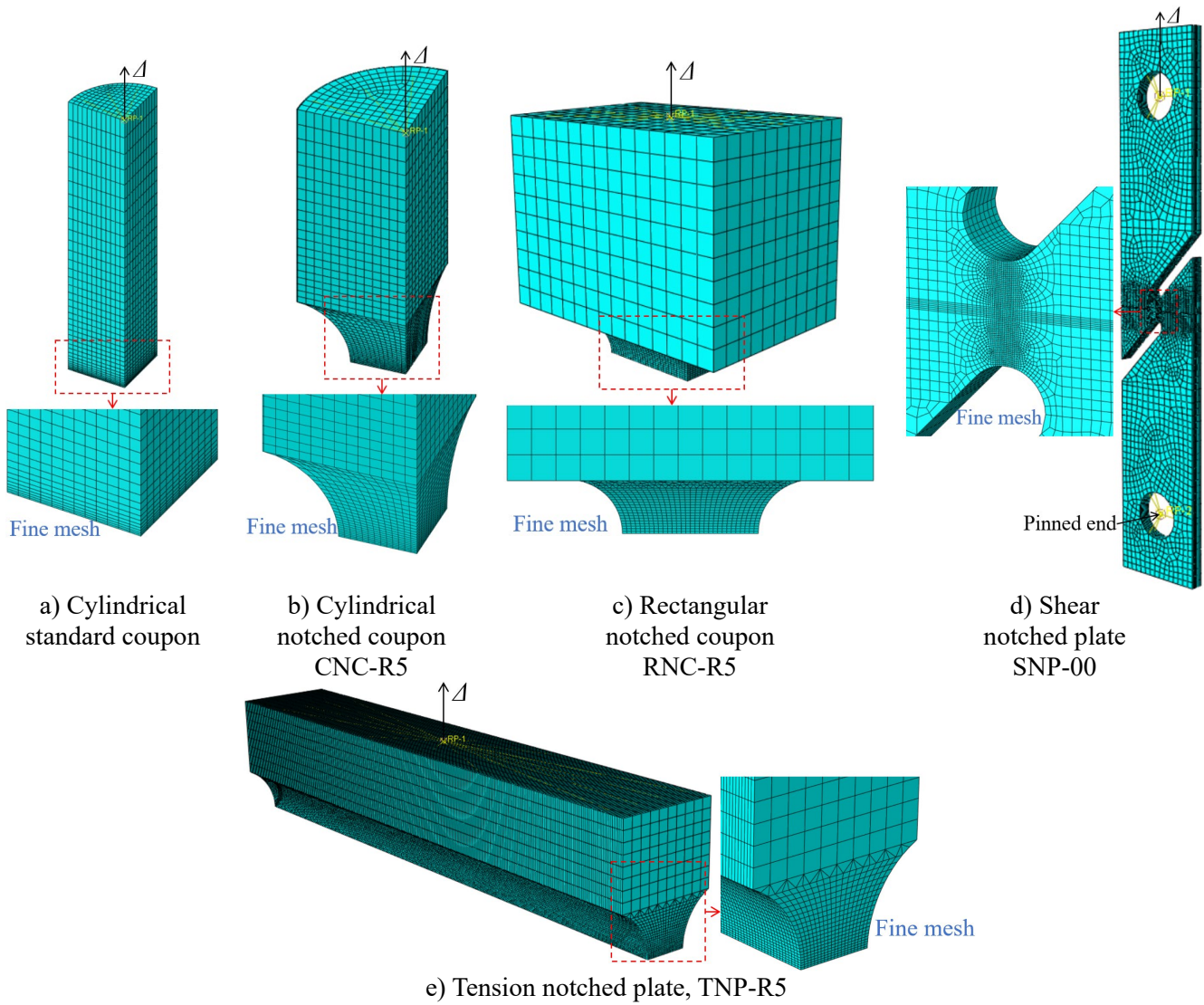


**Figure 5.7** Engineering stress-strain curve, CSC-1.





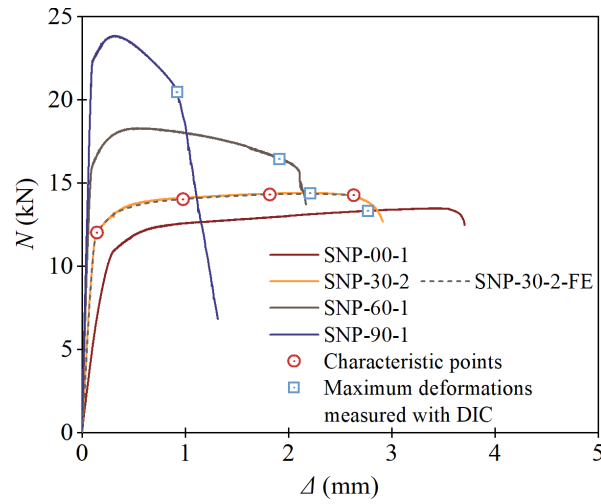
**Figure 5.8** True stress-strain curve, CSC-1.



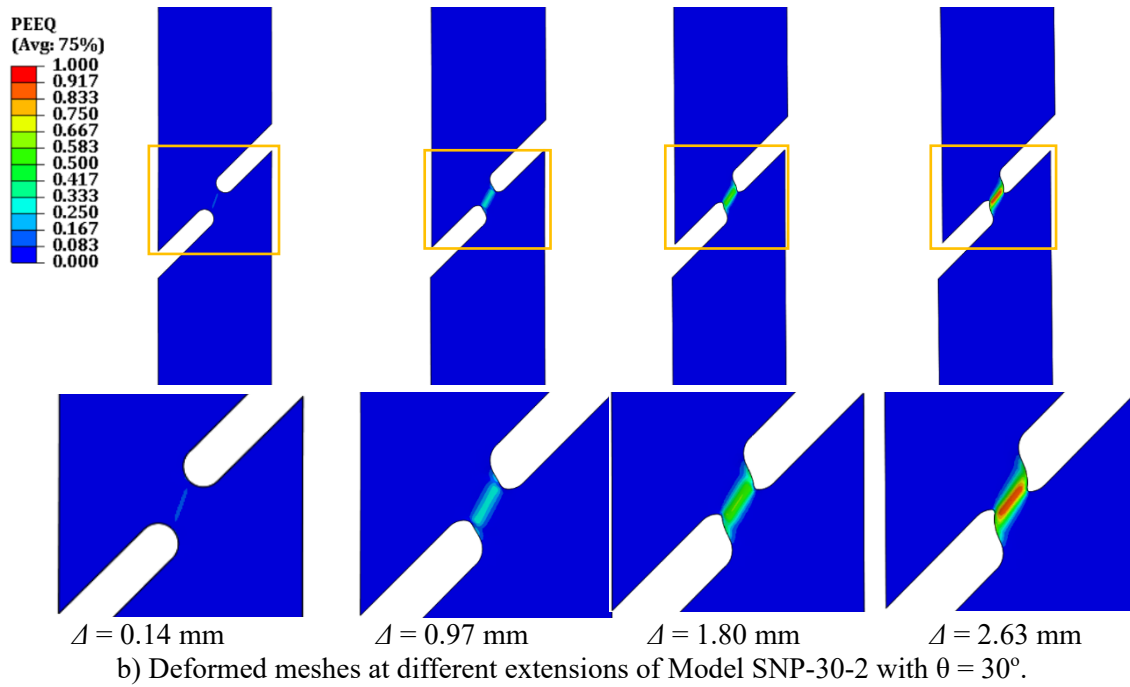
**Figure 5.9** Boundary condition and meshing strategy of numerical models.

### 5.3.2 Typical numerical results

As the deformation characteristics of the shear notched plates are very important in this study, **Figure 5.10** illustrates the numerical results of various Models SNP. As an example, the predicted load-extension curve of Model SNP-30 is plotted in **Figure 5.10a)** while deformed meshes at various extensions are illustrated in **Figure 5.10b)**. It is shown that the equivalent plastic strains PEEQ at the notched areas of the specimens are highly localized while their magnitudes increase as its extension is increased.



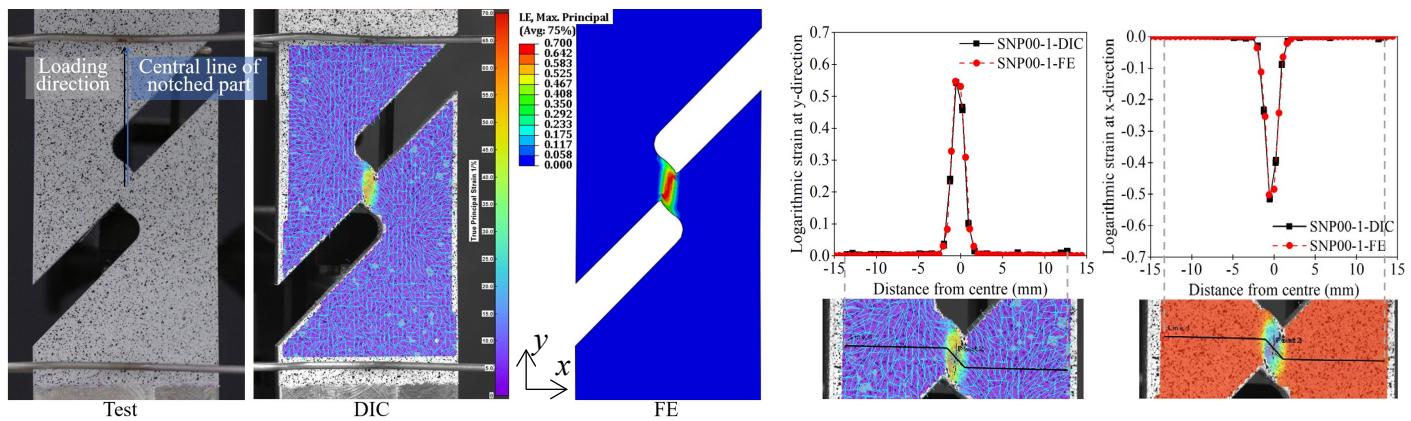
a) Predicted load-extension curves for various Models SNP



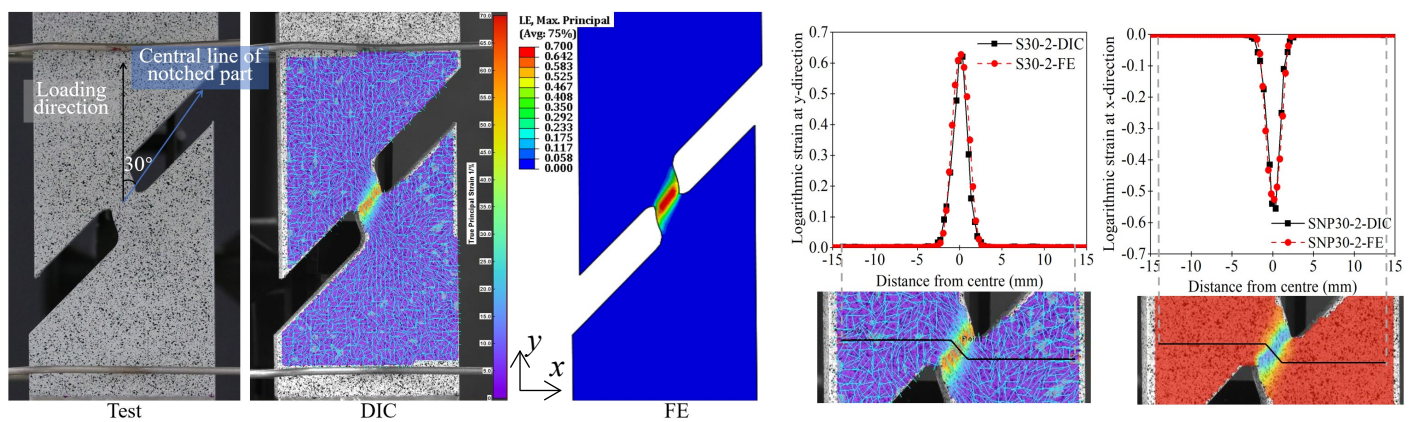
**Figure 5.10** Deformed shape of numerical model at different extensions. Plate SNP-30-2.

**Figure 5.11** presents the deformed shapes and the surface strain fields of four different shear notched plates, and these include:

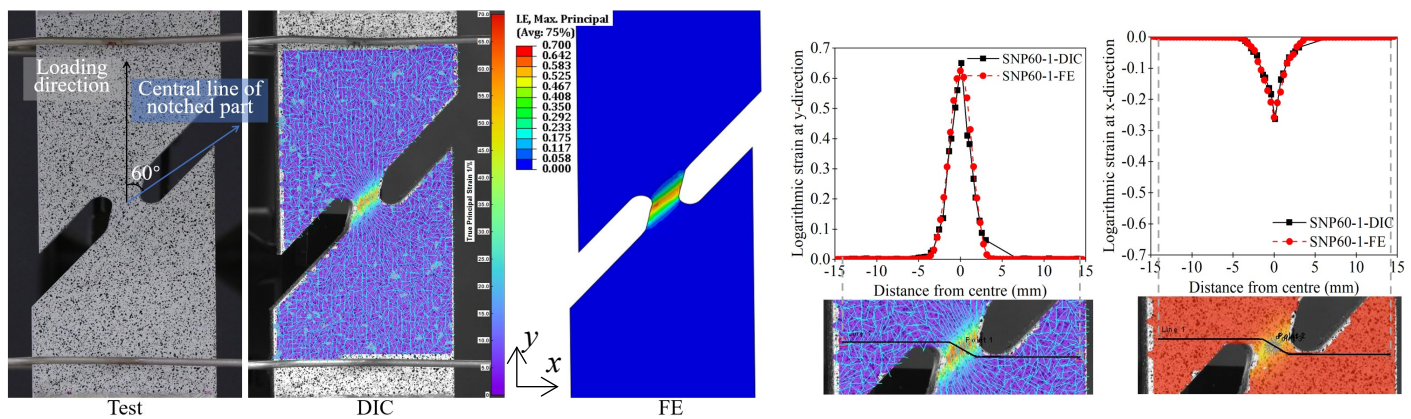
- i) deformed shapes of the specimens captured with a digital camera,
- ii) surface maximum principal strain fields of the specimens obtained with DIC,
- iii) the corresponding numerical results of both the deformed shapes and the surface strains,
- and
- iv) the distributions of both the longitudinal and the transverse strains in the vicinity of the notched areas of the specimens.



a). SNP-00-1 with  $\theta = 0^\circ$ . ( $\Delta=2.77\text{mm}$ )

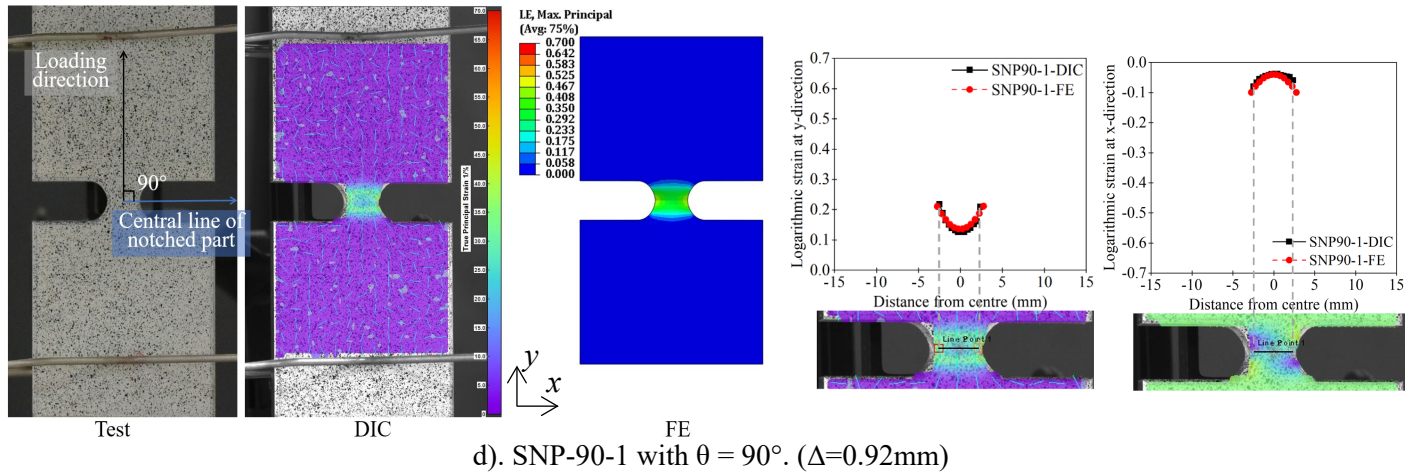


b). SNP-30-2 with  $\theta = 30^\circ$ . ( $\Delta=2.21\text{mm}$ )



c). SNP-60-1 with  $\theta = 60^\circ$ . ( $\Delta=1.91\text{mm}$ )





**Figure 5.11** Surface deformations and strain distributions of Shear Notched Plates.

It is demonstrated that

- By comparing the observed and the predicted deformed shapes of the specimens, a close agreement between the predicted deformations and the photographic evidence is obtained.
- By comparing the measured and the predicted surface strains of the specimens, good agreements in both magnitudes and distributions of the strains are also evident.
- Through a rational data analysis and interpretation, the DIC and the numerical data for both the longitudinal and the transverse strains in the vicinity of the notched areas of the specimens are plotted. Good comparisons among these data are evident.

This comprehensive validation establishes that the numerical models are able to predict various deformation characteristics of these specimens, including the overall deformed shapes, and highly localized surfaces strains, i.e. both longitudinal and transverse strains, in the vicinity of the notched areas, during tests with a high level of accuracy.

It is important to identify the locations of fracture initiation and the distributions of plastic strains at fracture of the models of various test specimens, as shown in **Figure 5.12**. For

those cylindrical standard and the cylindrical notched coupons, and also those rectangular notched coupons and the tension notched plates, fracture initiation always takes place at the centers of their minimum cross-sections. For those shear notched plates, fracture initiation always occurs at the notched areas of the specimens.

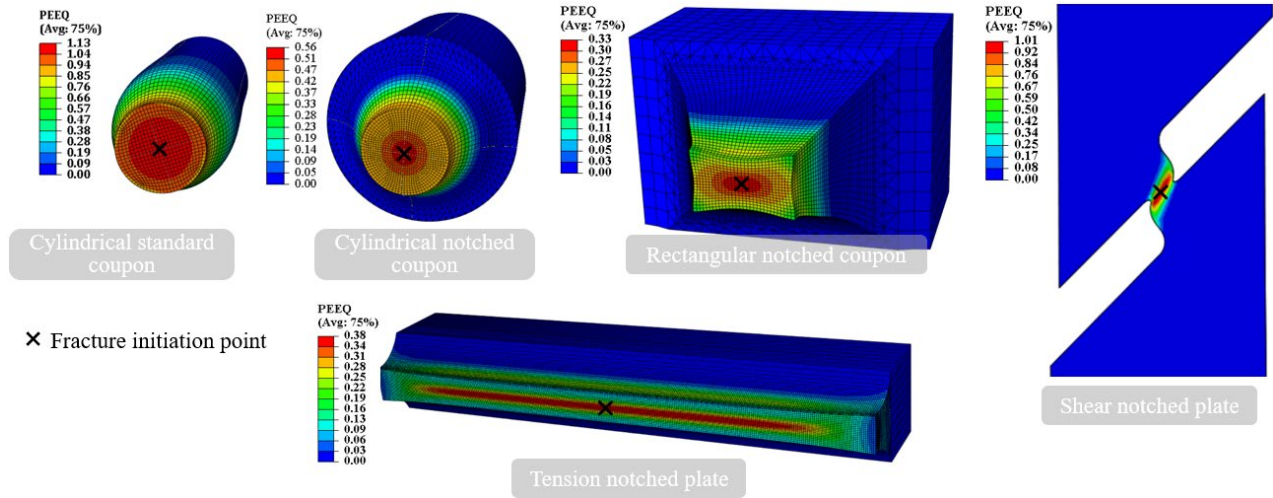


Figure 5.12 Fracture initiation locations of different specimens.

Figure 5.13 plots the predicted and the measured load-extension curves of various types of the test specimens, and a good agreement between the predicted and the measured results in both the linear and the non-linear deformation ranges is evident. Based on the test data, it is ready to identify the deformations of the test specimens at the point of fracture initiation, and the corresponding numerical results, in particular, those critical stresses and strains at fracture, are obtained. Consequently, the critical states of stresses and strains at fracture initiation of all these 34 specimens were extracted directly from the numerical models for subsequent analyses.

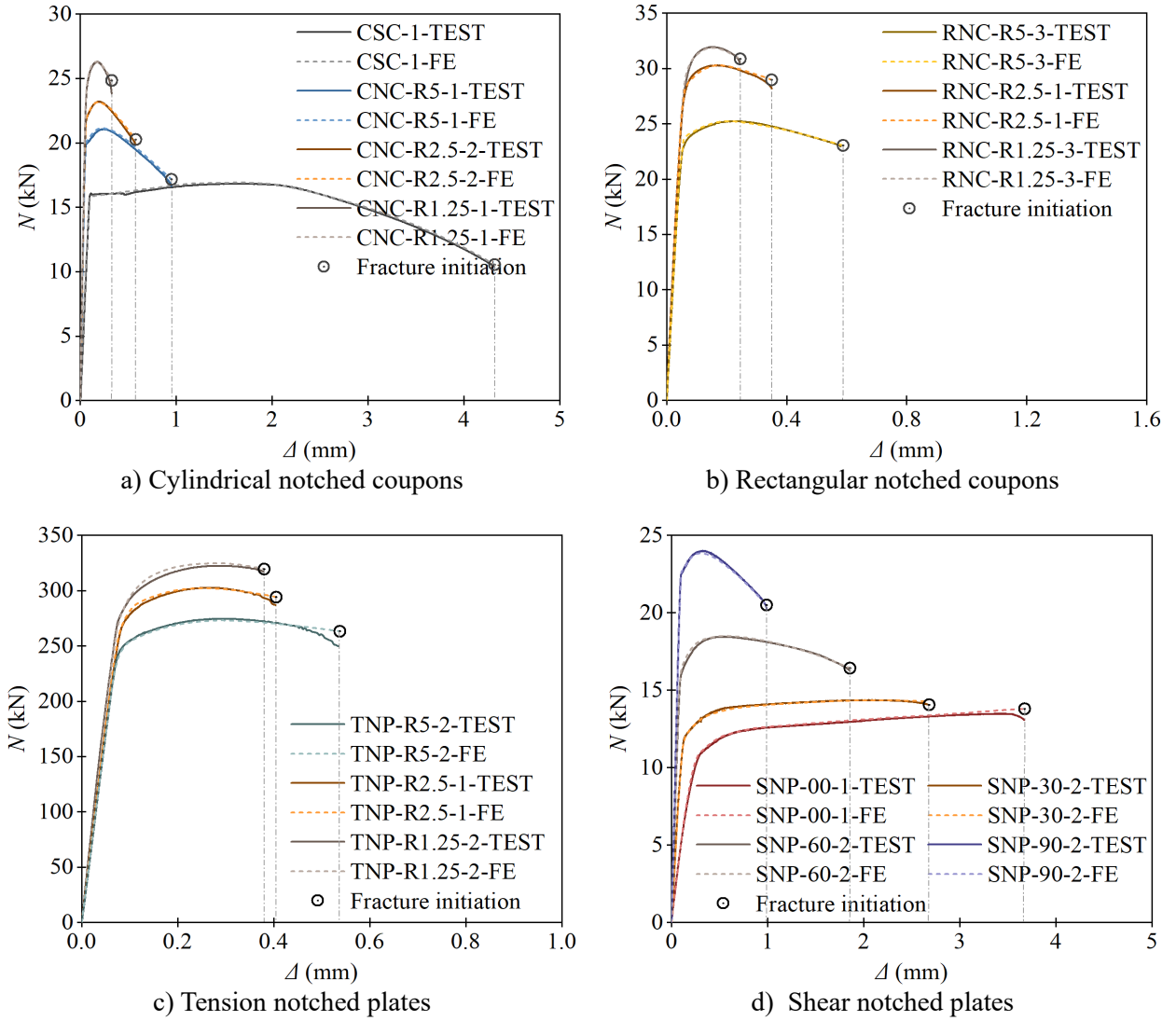


Figure 5.13 Load-extension curves from numerical simulations.

Since the stress triaxiality  $T$  and the Lode parameter  $L$  vary in tests, both an average stress triaxiality  $T_{av}$  and an average Lode parameter  $L_{av}$  are introduced to consider their variations during the entire course of loading:

$$T_{av} = \frac{1}{\varepsilon_f} \int_0^{\varepsilon_f} T d\varepsilon \quad (5-2)$$

$$L_{av} = \frac{1}{\varepsilon_f} \int_0^{\varepsilon_f} L d\varepsilon \quad (5-3)$$

The values of both the average stress triaxiality  $T_{av}$ , the average Lode parameter  $L_{av}$ , and the equivalent plastic strain at fracture, i.e.  $PEEQ_f$  or  $\varepsilon_f$  are summarized in Table 5-3, and they are considered as key parameters to describe the critical states of stresses and strains at fracture initiation of all these specimens.

**Table 5-3.** Critical states of stresses and strains at fracture initiation.

| Specimens                       |             | Stress triaxiality,<br>$T_{av}$ | Lode parameter,<br>$L_{av}$ | Fracture strain,<br>$\varepsilon_f$ |
|---------------------------------|-------------|---------------------------------|-----------------------------|-------------------------------------|
| Cylindrical<br>standard coupons | CSC-1       | 0.611                           | -1                          | 1.142                               |
|                                 | CSC-2       | 0.628                           | -1                          | 1.110                               |
| Cylindrical<br>notched coupons  | CNC-R5-1    | 0.948                           | -1                          | 0.513                               |
|                                 | CNC-R5-2    | 0.954                           | -1                          | 0.556                               |
|                                 | CNC-R5-3    | 0.949                           | -1                          | 0.518                               |
|                                 | CNC-R2.5-1  | 1.164                           | -1                          | 0.283                               |
|                                 | CNC-R2.5-2  | 1.174                           | -1                          | 0.328                               |
|                                 | CNC-R2.5-3  | 1.157                           | -1                          | 0.253                               |
|                                 | CNC-R1.25-1 | 1.468                           | -1                          | 0.158                               |
|                                 | CNC-R1.25-2 | 1.472                           | -1                          | 0.170                               |
|                                 | CNC-R1.25-3 | 1.472                           | -1                          | 0.170                               |
| Rectangular<br>notched coupons  | RNC-R5-1    | 0.995                           | -0.502                      | 0.436                               |
|                                 | RNC-R5-2    | 0.974                           | -0.517                      | 0.328                               |
|                                 | RNC-R5-3    | 0.964                           | -0.498                      | 0.315                               |
|                                 | RNC-R2.5-1  | 1.219                           | -0.455                      | 0.184                               |
|                                 | RNC-R2.5-2  | 1.205                           | -0.418                      | 0.192                               |
|                                 | RNC-R2.5-3  | 1.196                           | -0.406                      | 0.180                               |
|                                 | RNC-R1.25-1 | 1.523                           | -0.329                      | 0.098                               |
|                                 | RNC-R1.25-2 | 1.559                           | -0.317                      | 0.143                               |
|                                 | RNC-R1.25-3 | 1.554                           | -0.327                      | 0.128                               |
| Tension notched<br>plates       | TNP-R5-1    | 0.959                           | -0.014                      | 0.380                               |
|                                 | TNP-R5-2    | 0.954                           | -0.015                      | 0.345                               |
|                                 | TNP-R2.5-1  | 1.183                           | -0.023                      | 0.265                               |
|                                 | TNP-R2.5-2  | 1.187                           | -0.021                      | 0.291                               |
|                                 | TNP-R1.25-1 | 1.488                           | -0.034                      | 0.190                               |
| Shear notched<br>plates         | TNP-R1.25-2 | 1.489                           | -0.046                      | 0.140                               |
|                                 | SNP-00-1    | 0.052                           | -0.101                      | 1.007                               |
|                                 | SNP-00-2    | 0.052                           | -0.103                      | 1.017                               |
|                                 | SNP-30-1    | 0.119                           | -0.261                      | 0.944                               |
|                                 | SNP-30-2    | 0.118                           | -0.257                      | 0.918                               |
|                                 | SNP-60-1    | 0.259                           | -0.536                      | 0.912                               |
|                                 | SNP-60-2    | 0.250                           | -0.521                      | 0.806                               |
|                                 | SNP-90-1    | 0.738                           | -0.184                      | 0.422                               |
|                                 | SNP-90-2    | 0.750                           | -0.188                      | 0.470                               |

### 5.3.3 Influence of stress triaxiality and Lode parameter on ductile fracture

The stress states in Table 5-3 are readily plotted in the  $T_{av} - L_{av} - \varepsilon_f$  space, as shown in Figure 5.14. The analysis demonstrates a consistent inverse relationship between fracture strain  $\varepsilon_f$  and the stress triaxiality  $T_{av}$ , where higher triaxiality levels correspond to reduced material ductility. This relationship also follows an exponential pattern when comparing specimens with identical Lode parameters  $L_{av}$ , as shown in

- Figure 5.15a) for  $L_{av} = -1$  based on the data from the cylindrical standard and the cylindrical notched coupons, and also
- Figure 5.15b) for  $L_{av} = 0$  based on the data from the tension notched plates and the shear notched plates with  $\theta = 0^\circ$ .

Notably, the fracture strains under the condition of  $L_{av} = 0$  are significantly lower than those of  $L_{av} = -1$  for all values of triaxiality levels. This suggests that the presence of shear stresses under plane strain conditions promotes fracture initiation in these specimens.

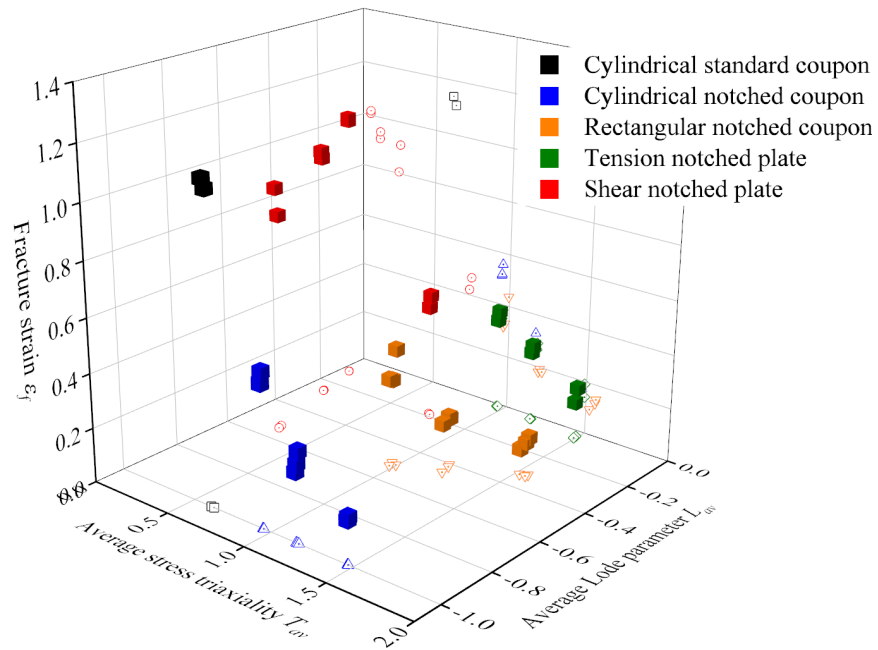
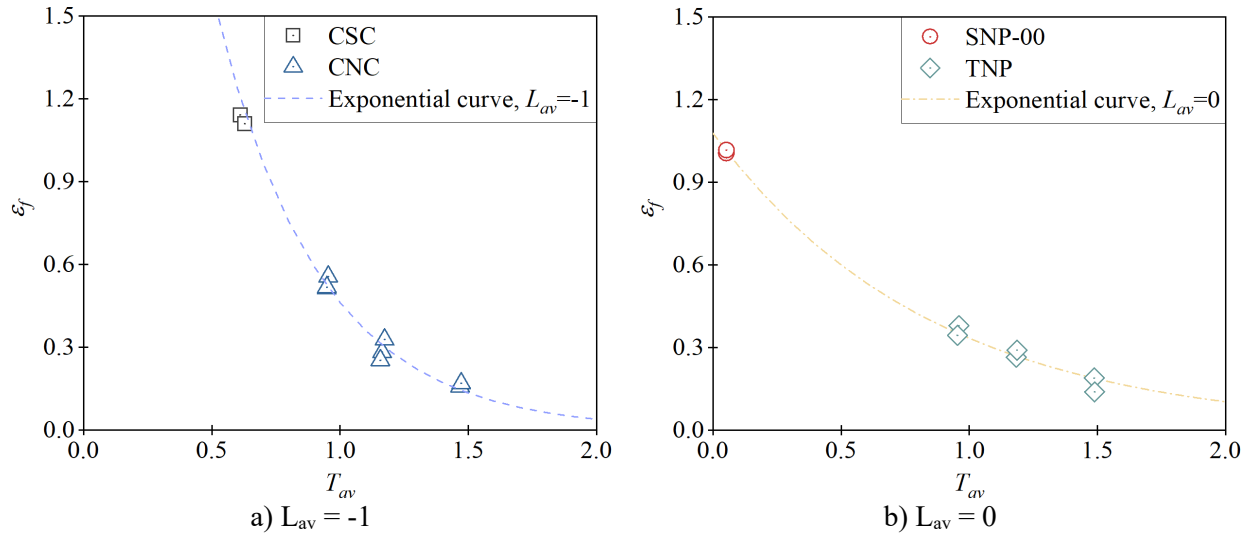


Figure 5.14 Critical stresses and strains in  $T_{av} - L_{av} - \varepsilon_f$  space.



**Figure 5.15** Effects of stress triaxiality onto fracture strains.

A relationship between the Lode parameter  $L_{av}$  and the stress triaxiality  $T_{av}$  with different combinations of tension and shear actions is revealed in **Figure 5.16**. It should be noted that the specimens with the same notched radii have deformed with almost the same values of stress triaxialities  $T_{av}$  at fracture initiation. It is interesting to compare that with the graph of the initial design, and it is apparent that many of these points have been shifted after large deformations. More importantly, **Figure 5.17** plots the non-linear variations between the fracture strains  $\varepsilon_f$  and the Lode parameter  $L_{av}$  with three different values of stress triaxiality  $T_{av}$ , i.e.  $T_{av} = 0.96, 1.19$  and  $1.5$ , corresponding to notched radii at 5, 2.5 and 1.25 mm respectively. These findings provide insights into the formulation of a fracture model to be proposed in the next section.

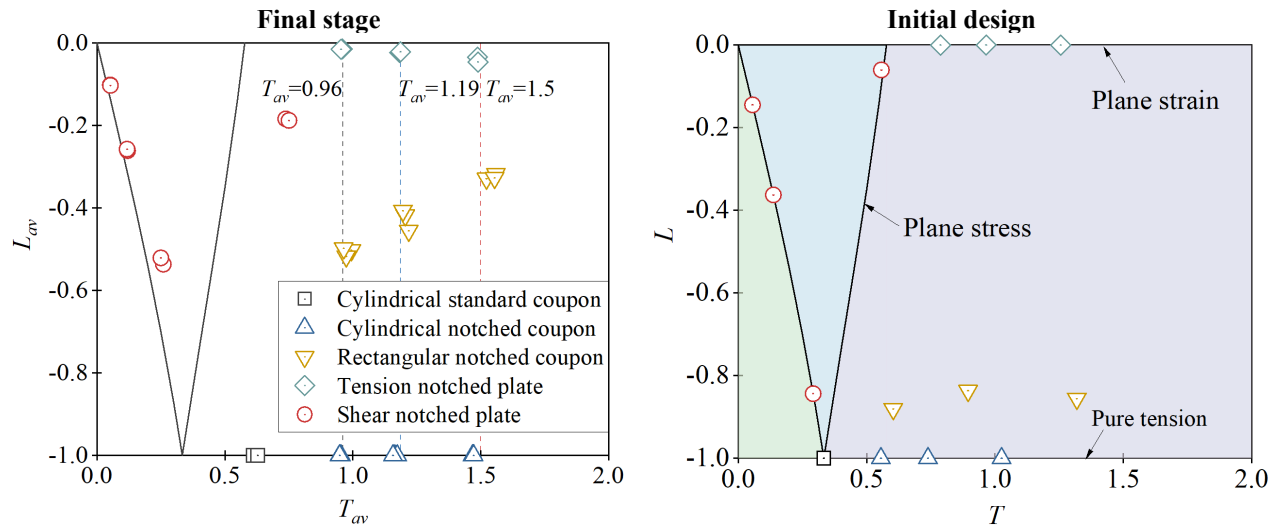


Figure 5.16 Critical stresses and strains in  $T_{av} - L_{av}$  space.

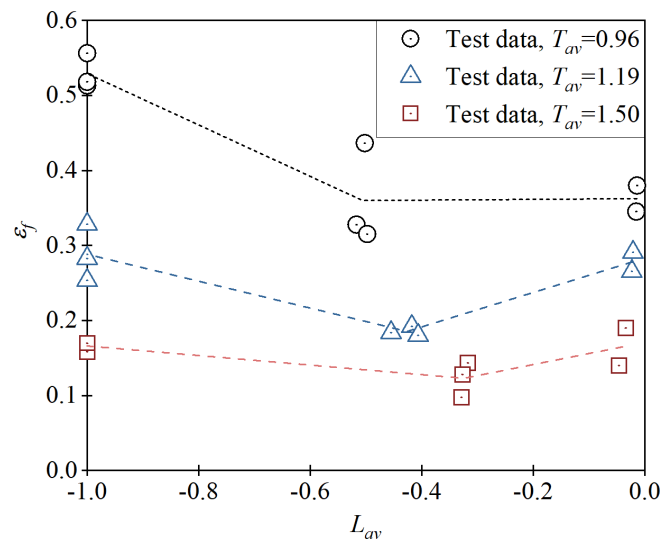


Figure 5.17 Critical stresses and strains in  $L_{av} - \varepsilon_f$  space.

## 5.4 Proposed fracture models

Five modern fracture models considering both the stress triaxiality and the Lode angle are adopted to evaluate the fracture behavior of the test specimens, which are Xue-Wierzbicki (X-W) model, the Bai-Wierzbicki (B-W) model, the Modified Mohr-Coulomb (MMC) model, the Lou-Huh (L-H) model and the Lode angle Modified Void Growth (LMVGM) model.

### 5.4.1 Comparisons with different models

In order to establish applicability of the modern fracture models presented in Table 2-1 to the S690 high strength steel, various parameters of these models are calibrated through a curve-fitting exercise with the critical states of stresses and strains at fracture initiation given in Table 5-3. The calibrated parameters are summarized in Table 5-4. It should be noted that accuracy of the curve-fitting exercise is determined with the value of coefficient  $R^2$ , which is given by:

$$R^2 = 1 - \frac{SSE}{SST} \quad (5-4)$$

where  $SSE$  is the residual sum of squares;  $SST$  is the total sum of squares. It should be noted that a larger value of  $R^2$  represents a higher fitting accuracy. In addition, it is important to assess accuracy of these models with calibrated parameters, and their prediction errors ER are listed in Table 5-4, which is given by:

$$ER = \frac{1}{N} \sum_{n=1}^N \left| \frac{\varepsilon_f^{pred} - \varepsilon_f^{test}}{\varepsilon_f^{test}} \right| \quad (5-5)$$



where  $N$  is the number of data points,  $\varepsilon_f^{test}$  is the fracture strain obtained from a test, and  $\varepsilon_f^{pred}$  is the fracture strain predicted with a model. Notably, the Xue-Wierzbicki model gives the most accurate results with a prediction error at 13.1% while the prediction errors of both the Lou-Huh and the LMVG models exceed 25%.

**Table 5-4** Calibrated parameters for modern fracture models.

| Fracture models             | Parameters                             |                                        |                                        |                                        |               | $R^2$        | Prediction error ER |
|-----------------------------|----------------------------------------|----------------------------------------|----------------------------------------|----------------------------------------|---------------|--------------|---------------------|
| Xue-Wierzbicki model        | $d_1$<br>8.55                          | $d_2$<br>2.78                          | $d_3$<br>1.17                          | $d_4$<br>1.26                          | $m$<br>16     | 0.966        | 13.1%               |
| Bai-Wierzbicki model        | $D_1$<br>2.76                          | $D_2$<br>1.76                          | $D_3$<br>1.03                          | $D_4$<br>1.24                          |               | 0.924        | 17.8%               |
| Modified Mohr-Coulomb model | $K$<br>1098                            | $n$<br>0.06                            | $c_1$<br>0.08                          | $c_2$<br>704.56                        | $c_s$<br>0.91 | 0.927        | 16.2%               |
| Lou-Huh model               | $g_1$<br>5.73                          | $g_2$<br>1.34                          | $g_3$<br>1.29                          |                                        |               | 0.828        | 28.7%               |
| LMVGM                       | $\delta$<br>0.96                       | $\beta$<br>1.27                        | $\gamma$<br>0.53                       |                                        |               | 0.868        | 25.7%               |
| <b>Proposed model</b>       | <b><math>C_1</math></b><br><b>5.43</b> | <b><math>C_2</math></b><br><b>2.46</b> | <b><math>C_3</math></b><br><b>1.08</b> | <b><math>C_4</math></b><br><b>1.17</b> |               | <b>0.971</b> | <b>11.8%</b>        |

It is highly desirable to present each of these models with a fracture locus in the  $T_{av} - L_{av} - \varepsilon_f$  space which describes an initiation of fracture in the steel. Hence, when the equivalent plastic strain PEEQ within the models reaches any point of the fracture locus, an initiation of fracture occurs. The calibrated fracture locus is plotted in the  $T_{av} - L_{av} - \varepsilon_f$  space, as shown in [Figure 5.18a](#) to [Figure 5.22a](#)). In order to illustrate specific features of these models, the fracture loci are projected to the  $T_{av} - \varepsilon_f$  planes so that the pure tension conditions, i.e.  $L_{av} = -1$ , are shown in [Figure 5.18b](#) to [Figure 5.22b](#)) while the plane strain conditions, i.e.  $L_{av} = 0$ , are shown in [Figure 5.18c](#) to [Figure 5.22c](#)). It should be noted that:

- As shown in Figure 5.18, Figure 5.19 and Figure 5.20, the Xue-Wierzbicki model, the Bai-Wierzbicki model, and the Modified Mohr-Coulomb model are considered to be able to give good predictions as their fracture loci follow closely to almost all test data. In those regions of small  $T_{av}$  and large  $L_{av}$ , these three models exhibit some degrees of sensitivity to the Lode parameter in such a way that declines in fracture strains under pure tension conditions far exceed those in fracture strains under plane strain conditions.
- However, it is shown in Figure 5.21 and Figure 5.22 that both the Lou-Huh model and the LMVGM model are not able to give good predictions as their fracture loci do not follow many of the test data. This deficiency leads to prediction errors exceeding 50% when  $T_{av}$  exceeds 1.0, and hence, they are not suitable for engineering applications involving complex loading conditions.

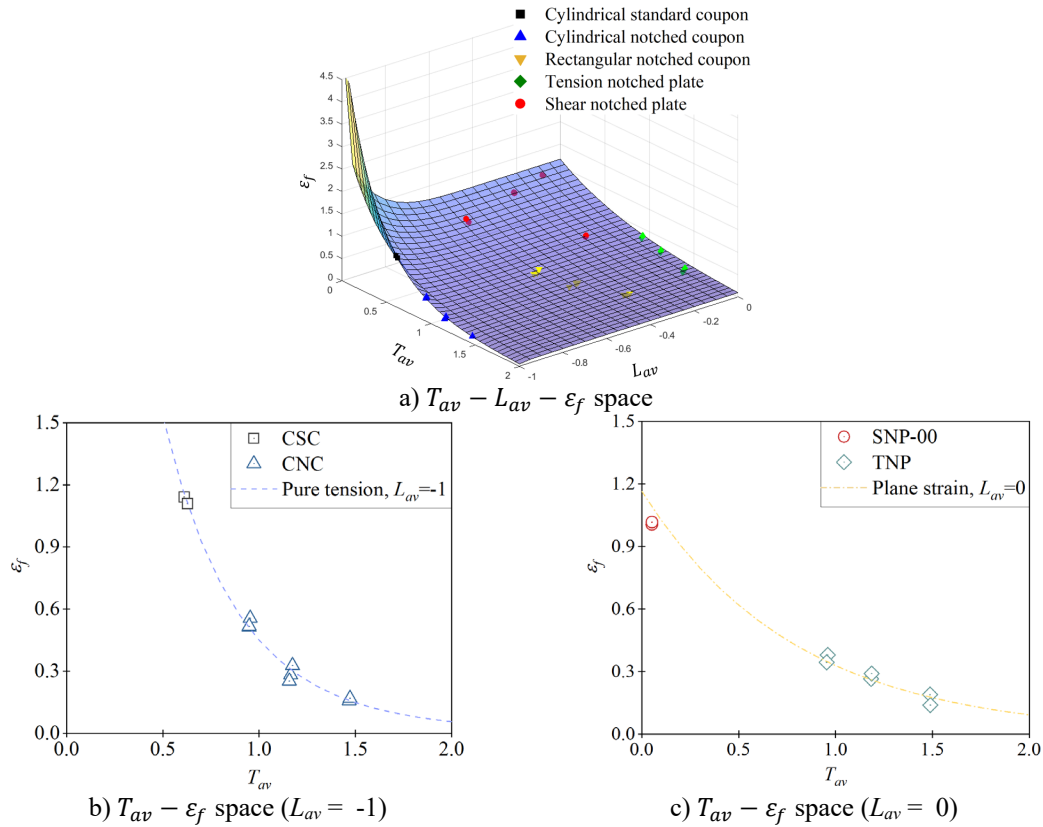


Figure 5.18 Fracture locus of Xue-Wierzbicki model.

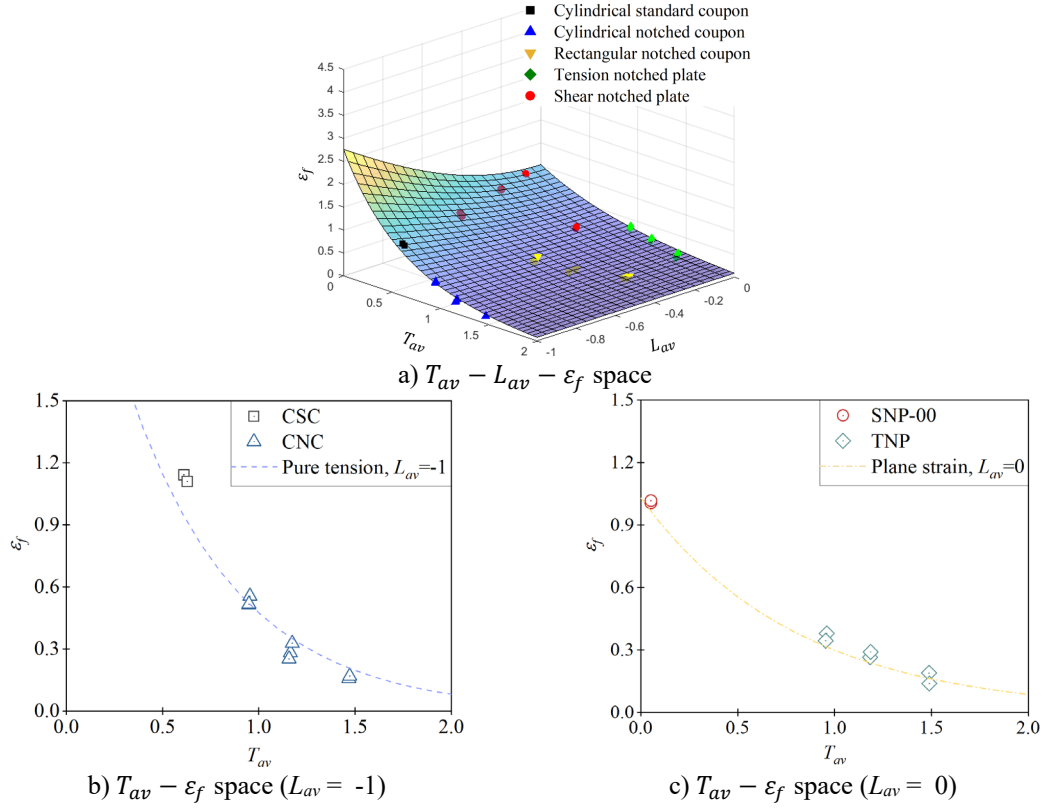


Figure 5.19 Fracture locus of Bai-Wierzbicki model.

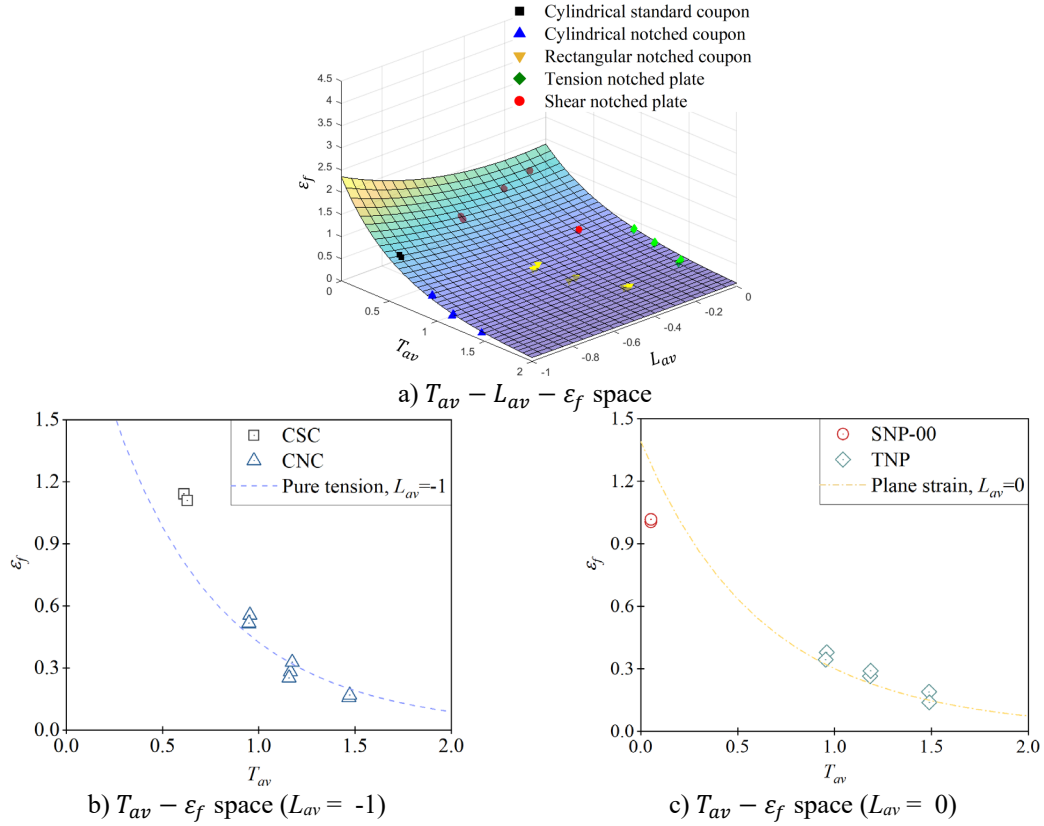


Figure 5.20 Fracture locus of Modified Mohr-Coulomb model.

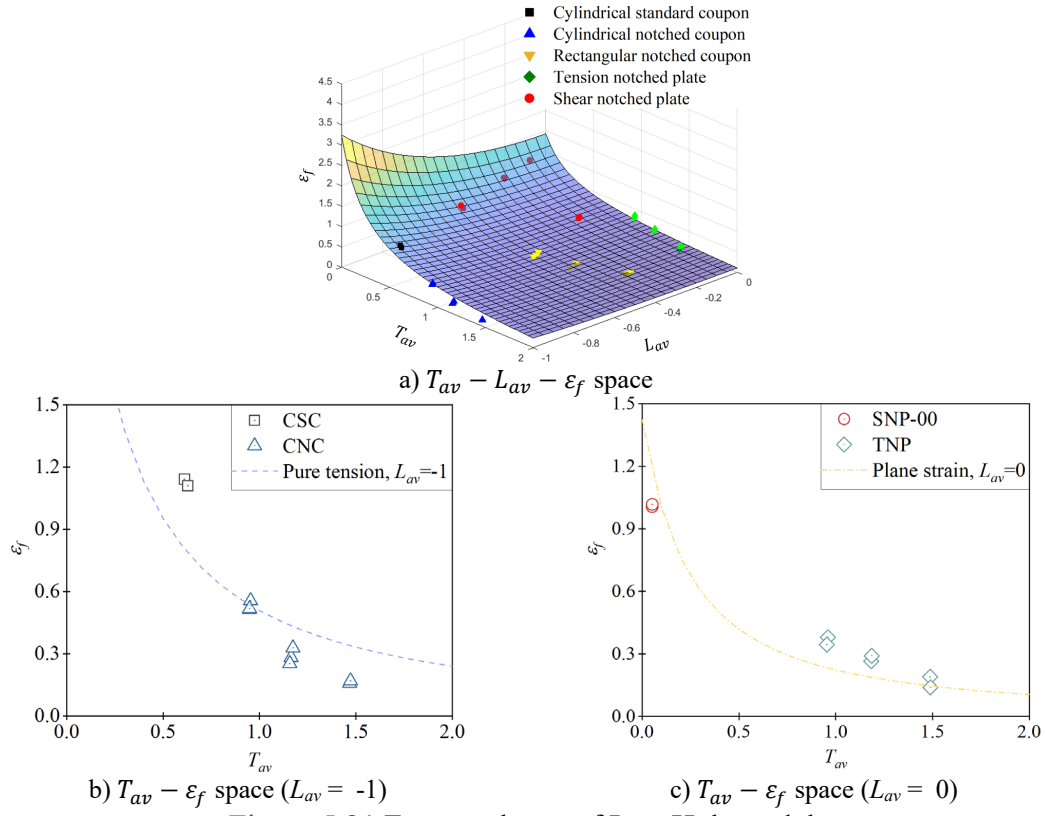


Figure 5.21 Fracture locus of Lou-Huh model.

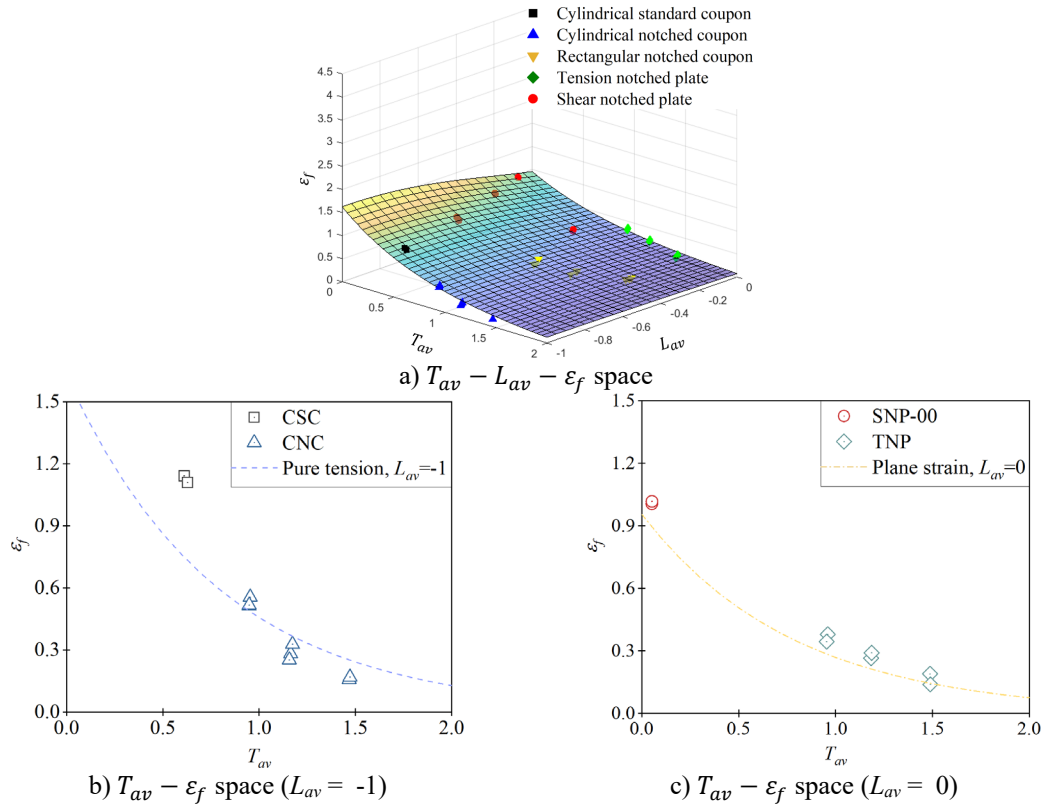
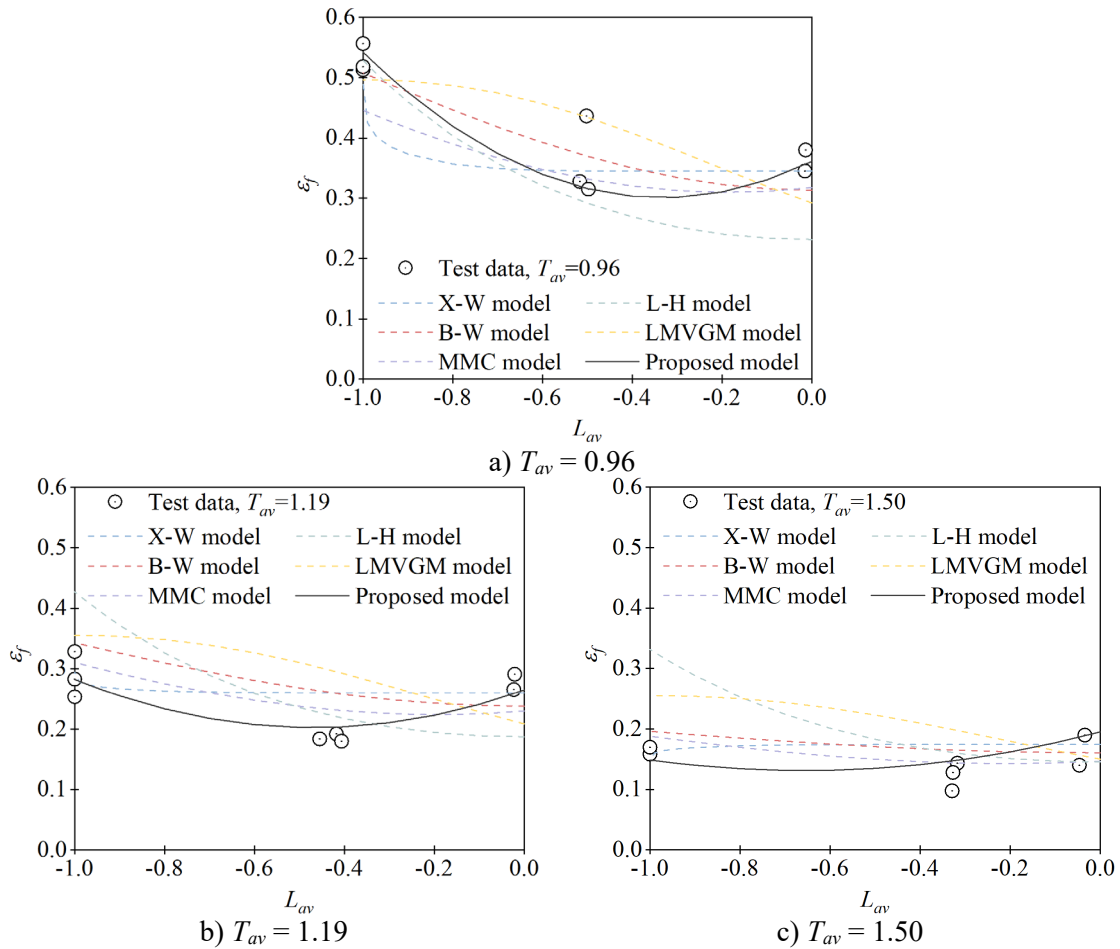


Figure 5.22 Fracture locus of LMVGM model.

The fracture loci of the five models at the same levels of stress triaxiality, i.e.  $T_{av} = 0.96$ , 1.19 and 1.50, are plotted onto the same graphs in **Figure 5.23**. As previously discussed in **Section 5.4**, a non-linear relationship between the Lode parameter and the fracture strains is evident. For each of the graphs with specific values of  $T_{av}$ , the fracture strains are found to decrease first and then increase, with different extents, with an increase in  $L_{av}$ . However, none of these models have demonstrated such a trend, and these detailed comparisons highlight a significant deficiency in the models when being applied to the S690 steel. Consequently, these findings underscore the need for developing a fracture model which is able to describe accurately the unique fracture behaviour of the S690 steel under combined tension and shear actions.



**Figure 5.23** Fracture loci at  $L_{av} - \varepsilon_f$  space.

### 5.4.2 New ductile fracture model for S690 steel

A new fracture model is proposed for the S690 high strength steel. As shown in [Figure 5.15](#), both the pure tension and the plane strain states are two key boundaries of the fracture locus of the proposed model, and they are readily expressed with the following exponential functions:

$$\text{Pure tension state:} \quad \varepsilon_f^{tn} = C_1 e^{-C_2 T_{av}} \quad (5-6)$$

$$\text{Plane strain state:} \quad \varepsilon_f^{ps} = C_3 e^{-C_4 T_{av}} \quad (5-7)$$

where  $C_1$ ,  $C_2$ ,  $C_3$  and  $C_4$  are independent parameters. Hence, the fracture locus is defined as a combination of these two exponential functions. Since the fracture strain varies roughly in a quadratic manner with Lode parameter (see [Figure 5.17](#)), the fracture strain may be expressed as follows:

$$\varepsilon_f = A \cdot L_{av}^2 + B \cdot L_{av} + C \quad (5-8)$$

When  $L = 0$ ,  $\varepsilon_f$  equals  $\varepsilon_f^{ps}$ ; therefore,  $C$  equals  $\varepsilon_f^{ps}$ .

When  $L = -1$ ,  $\varepsilon_f$  equals  $\varepsilon_f^{tn}$ , and  $A - B + C = \varepsilon_f^{tn}$ . Set  $B$  equals  $\varepsilon_f^{ps}$ , and  $A$  becomes  $\varepsilon_f^{tn}$ .

Therefore, the fracture strain is given by:

$$\begin{aligned} \varepsilon_f &= \varepsilon_f^{tn} \cdot L_{av}^2 + \varepsilon_f^{ps} \cdot L_{av} + \varepsilon_f^{ps} \\ &= C_1 e^{-C_2 T_{av}} \cdot L_{av}^2 + C_3 e^{-C_4 T_{av}} \cdot L_{av} + C_3 e^{-C_4 T_{av}} \end{aligned} \quad (5-9)$$

After calibration against the test data, parameters  $C_1$ ,  $C_2$ ,  $C_3$  and  $C_4$  are summarized in [Table 5-4](#), and the fracture locus of the proposed model is plotted in the  $T_{av} - L_{av} - \varepsilon_f$  space, as shown in [Figure 5.24](#). For a direct comparison with the five modern fracture models, the fracture locus of the proposed model at specific stress triaxiality, i.e.  $T_{av} = 0.96$ ,

1.19 and 1.50, are also plotted in various graphs of the  $L_{av} - \varepsilon_f$  space in Figure 5.23. It is evident that the proposed model is able to predict the fracture strains in a highly satisfactory manner. Accuracy of the proposed model is also evaluated as described in Section 5.4.1, and both  $R^2$  and  $ER$  are also given in Table 5-4. It is shown that proposed model has the highest accuracy with a prediction error at merely 11.8%.

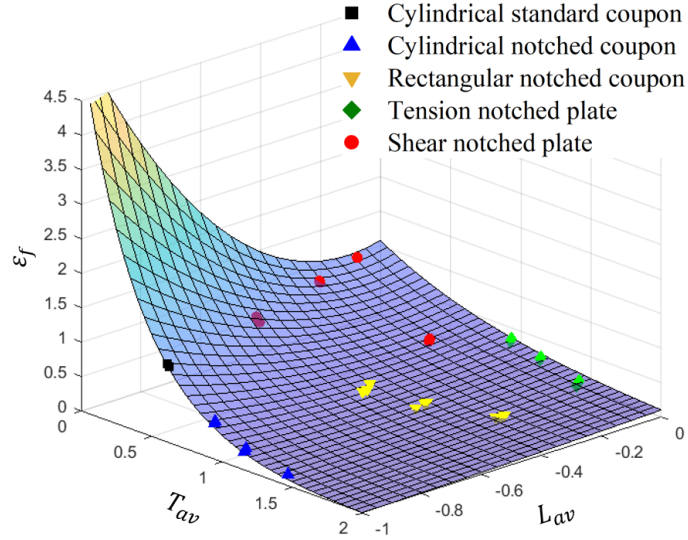


Figure 5.24 Fracture locus of the proposed model.

### 5.4.3 Sensitivity study

A sensitivity study was conducted to evaluate the influence of the model parameters  $C_1$ ,  $C_2$ ,  $C_3$ , and  $C_4$  on the prediction accuracy of the proposed ductile fracture model. In this study, each parameter was individually varied by  $\pm 10\%$  from its calibrated value, and the corresponding prediction errors with respect to the experimental results were calculated.

As shown in Table 5-5, modifying  $C_2$  presents the largest effect on the prediction accuracy of the proposed model. When  $C_2$  is increased by 10%, the prediction error (ER) is increased to 15.9%, while a 10% reduction in  $C_2$  results in an increase at 18.7%. The corresponding

increases in the coefficient of variation (COV) are found to be 34.5% and 58.2% respectively. These results clearly demonstrate that the prediction accuracy of the proposed model is most sensitive to the parameter  $C_2$  while a change in the values  $C_1$ ,  $C_3$ , and  $C_4$  demonstrates a moderate impact. This finding underscores the importance of a precise evaluation of  $C_2$  to ensure reliable simulation results.

**Table 5-5** Sensitivity study.

| Parameters          | $C_1$ |      |       | $C_2$ |       |       | $C_3$ |      |       | $C_4$ |       |       |
|---------------------|-------|------|-------|-------|-------|-------|-------|------|-------|-------|-------|-------|
| Variation           | +10%  | 0    | -10%  | +10%  | 0     | -10%  | +10%  | 0    | -10%  | +10%  | 0     | -10%  |
| Value               | 5.97  | 5.43 | 4.89  | -2.71 | -2.46 | -2.22 | 1.19  | 1.08 | 0.97  | -1.29 | -1.17 | -1.05 |
| Prediction error ER | 13.1% | -    | 12.4% | 15.9% | -     | 18.7% | 12.9% | -    | 13.6% | 13.3% | -     | 14.6% |
| COV                 | 10.8% | -    | 4.8%  | 34.5% | -     | 58.2% | 9.4%  | -    | 15.4% | 12.5% | -     | 23.7% |

## 5.5 Conclusions

In this chapter, the effects of combined tension and shear actions on the ductile fracture behaviour of S690 high strength steel were investigated through experimental and numerical approaches. Both the stress triaxiality  $T$  and the Lode parameter  $L$  are introduced to evaluate effects of stress concentration and deviatoric stresses separately. Five types of notched specimens with different levels of stress triaxialities and Lode parameters were designed to decouple their effects on the specimens. Tensile tests on a total of 34 notched specimens were conducted carefully to capture their deformations up to fracture. Visual examination on fractured specimens using scanning electron microscopy (SEM) was also conducted to identify microstructural differences between tension- and shear-dominated fracture. Advanced numerical modelling using three dimensional solid elements was then carried out to simulate structural behaviour of these notched specimens up to fracture, and *critical states of both stresses and strains* for each of these coupons at fracture were



identified. Based on the numerical models and a wide range of the Lode parameters obtained above, a new ductile fracture model is proposed. It should be concluded that:

- 1) A total of 34 test specimens of the S690 steel with various shapes and notch dimensions have been successfully tested under tension. Among all these specimens, the values of the stress triaxiality  $T_{av}$  are specified to range from 0 to 1.5 while those of Lode parameter  $L_{av}$  are specified to range from -1 to 0. Moreover, 20 specimens were tested to provide data on tension actions, 2 on shear actions and 12 on combined tension and shear actions.
- 2) Fracture surfaces were observed with SEM. The result shows that the ductile fracture triggered by shear actions has a different microstructure, i.e. no dimples, when compared with those by tension actions. When the ductile fracture is caused by a combination of tension and shear actions, both dimples and layered features are apparent.
- 3) The influence of the stress triaxiality  $T$  and the Lode parameter  $L$  on the fracture strain  $\varepsilon_f$  was examined through advanced numerical simulation. In general, the fracture strain  $\varepsilon_f$  was found to increase with a decrease in the stress triaxiality  $T$ , and the increase in the fracture strains  $\varepsilon_f$  at the pure tension state is more rapid than that at the plane strain state. With an increase in the Lode parameter  $L$ , the fracture strain  $\varepsilon_f$  is found to decrease first, and then increase.
- 4) After calibrating a total of five modern ductile fracture models with the test data of this study, the fracture surfaces of these models are obtained together with the prediction errors. It is shown that all of these models fail to describe the fracture behavior of these test specimens of the S690 steel satisfactorily.

- 5) A new fracture model was thus proposed after a full consideration of the non-linear relationship between the fracture strain and the Lode parameter according to the test data of this study. The proposed model was shown to be able to describe the ductile fracture of the S690 steel under a wide range of combined tension and shear actions commonly encountered in practical situations with a high level of accuracy.

It should be noted that while the overall methodology of the proposed model is applicable to all structural steel, the proposed model is currently only applicable to the S690 high strength steel because of availability of calibrated parameters. With the availability of similar sets of test data of other steel, the parameters of the proposed model should then be calibrated carefully to extend the applicability of the proposed model.

## Chapter 6

### Ductile Fracture Modelling of S690 Bolted Connections

#### 6.1 Introduction

Bolted connections, widely used for their constructability and adaptability, are particularly vulnerable to ductile fracture in their net sections under complex stress conditions, i.e., combined tension and shear stresses, in the vicinity of bolt holes. This chapter focuses on calibrating and validating the proposed ductile fracture model in [Chapter 5](#) to predict both fracture initiation and crack propagation of typical S690 bolted connections. Six different types of notched coupons were designed and tested under tensile loadings to generate different combinations of tension and shear actions. Examination on fractured specimens using scanning electron microscopy (SEM) was also conducted to identify microstructural differences between fracture triggered by tension and by shear. Advanced numerical models were also established to simulate structural behaviour of these notched coupons up to fracture, and critical states of both stresses and strains for each of these coupons at fracture were identified to calibrate the proposed model. Then, the ductile fracture behaviour of the bolted connections under monotonic loadings was predicted by the calibrated model. Both locations and deformations of the bolted connections at fracture initiation were predicted, and the path of fracture propagation within the bolted connections during the whole loading process was simulated by deleting fractured elements within the model.

## 6.2 Failure modes of structure members and connections

In steel structures, structural members and connections often experience complex stress states due to geometrical discontinuities, such as welds, notches, abrupt cross-sectional transitions, and changes in loading directions. In many of these connections, they are particularly prone to initiation of ductile fracture since the difference of principal stresses ( $\sigma_1 - \sigma_3$ ) caused by both geometrical discontinuities and changes in loading directions will greatly influence the behaviour of ductile fracture in the way of promoting stress concentration and deviatoric stresses [34, 35, 84]. To be specific, geometric discontinuities, such as small-radius weld fillets, generate localized stress gradients at notch roots due to multi-axial stress states. In cases of multi-axial actions such as T-joints subjected to combined axial tension and in-plane bending actions, the angle between the loading direction and the lengthwise direction of the chord or the brace drives principal stress re-orientation. In such cases, an axial action establishes uniform tensile stresses in the chord wall, while bending moments transmitted through welds induce in-plane distortions, resulting in a coupled tensile-shear stress state near the weld toe that raises local differences in the principal stresses, and thus pre-conditioning these regions to fracture initiation.

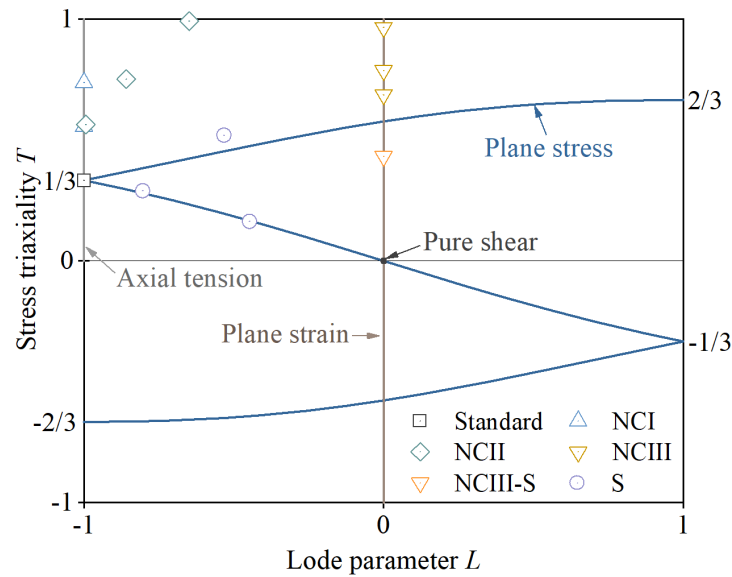
The difference of principal stresses resulting in stress concentration and deviatoric stresses is a key parameter to determine the failure mode of ductile fracture, which include tension failure, shear failure, and combined tension-shear failure. The stress triaxiality  $T$  is a crucial factor in quantifying the effect of stress concentration as follows:

- When  $T$  is large, the mean principal stress dominates, leading to a tension failure mode.
- When  $T$  is small, deviatoric stresses become more significant, and a combined tension-shear failure will occur if the shear stress is involved.

Moreover, a Lode parameter  $L$  represents the distribution of deviatoric stresses, affecting the failure mode under complex stress states as follows:

- A Lode parameter of 1 indicates an axial tension state, and hence, a tensile failure will always take place.
- A Lode parameter of 0 corresponds to a pure shear or a plane strain state, and hence, a shear failure will occur if only shear stresses are involved; otherwise, a combined tension-shear failure will take place.

All the above-mentioned stress states are illustrated in the  $L$ - $T$  space, as shown in **Figure 6.1**. **Table 6-1** summarizes all potential failure modes under various  $T$  and  $L$  conditions. It should be noted that the ductile fracture is always dominated by stress triaxiality since the failure mode remains unchanged when  $T > 2/3$ . In general, a Lode parameter only has impact at medium to low stress triaxiality, i.e.  $0 < T < 2/3$ .



**Figure 6.1** Space of Lode parameter  $L$  and stress triaxiality  $T$ .

**Table 6-1** Potential failure modes of ductile fracture under different stress states.

|              | $0 < T < 1/3$           | $1/3 < T < 2/3$         | $T > 2/3$       |
|--------------|-------------------------|-------------------------|-----------------|
| $L = -1$     | Tension failure         | Tension failure         | Tension failure |
| $-1 < L < 0$ | <i>Combined failure</i> | <i>Combined failure</i> | Tension failure |
| $L = 0$      | <b>Shear failure</b>    | <i>Combined failure</i> | Tension failure |

### 6.3. Experiments on notched coupons under tension

#### 6.3.1 Design of coupons

A series of coupon specimens are designed to capture different stress states at their critical positions of fracture, and their designed configurations are illustrated in [Figure 6.2](#). The design rationale is presented as follows:

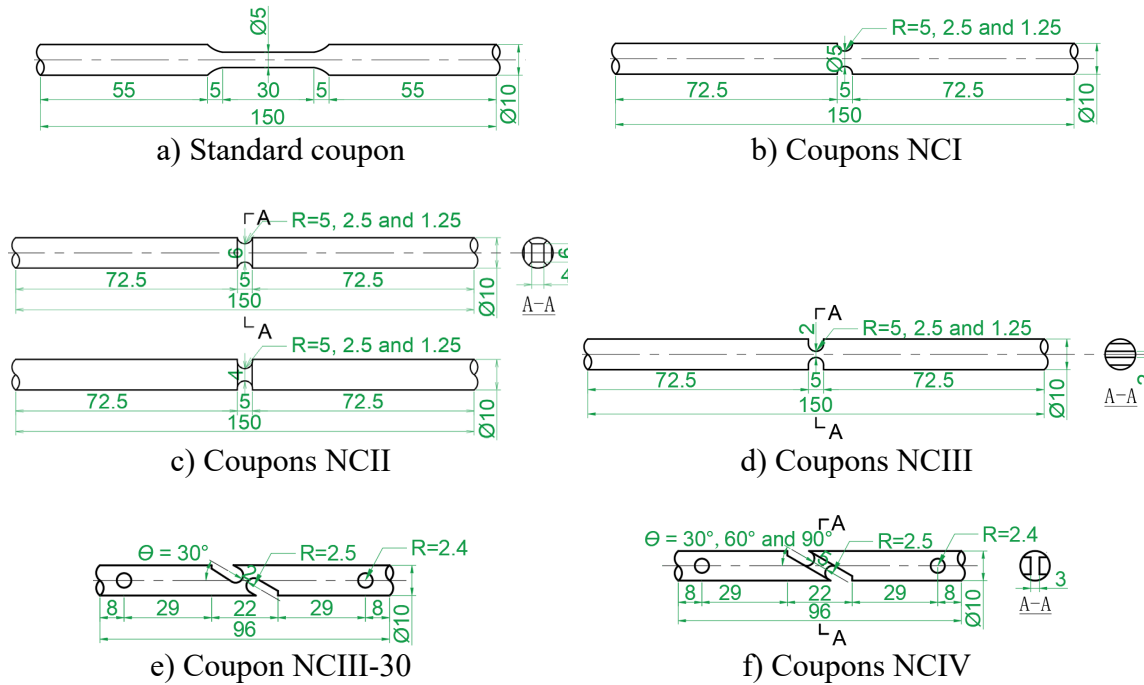
- 1) Standard coupon: As shown in [Figure 6.2a](#)), it is designed to provide mechanical properties of the steel plate, and fracture is expected to be initiated in its parallel section whose initial stress state is uniaxial tension.
- 2) Notched coupons with cylindrical cross-sections (NCI): All of these coupons have a circular cross-section that provides a pure tension stress state at their minimum cross sections. They are fabricated according to three different notch radii, i.e. 5.0, 2.5 and 1.25 mm, to generate different stress triaxiality at their centres, as shown in [Figure 6.2b](#)). These coupons are designated as Coupons NCI-R5, NCI-R2.5 and NCI-R1.25, respectively. Since the stress distribution is always uniform in the circumferential direction of these cross sections, their Lode parameter equals 1.0.
- 3) Notched coupons with rectangular cross-sections (NCII): Similar to the notched coupons with cylindrical cross-sections, these coupons are fabricated with the same rectangular cross-sections but different notch radii, i.e. 5.0, 2.5 and 1.25 mm, as shown in [Figure 6.2c](#)). The stress distribution at their minimum cross-sections is non-uniform

at all directions, so both the stress triaxiality and the Lode parameter vary with the notch radius. These coupons are designated according to their notch radii, i.e. Coupons NCII-R5, NCII-R2.5 and NCII-R1.25 respectively.

- 4) Grooved coupons (NCIII): In these coupons, a pair of symmetrical grooves are machined at their mid-lengths, as shown in [Figure 6.2d](#)), and their groove radii are taken to be 5.0, 2.5 and 1.25 mm for Coupons NCIII-R5, NCIII-R2.5 and NCIII-R1.25, respectively. Since the widths of the notched parts of the coupons are devised to be 10 mm, they are five times of their thickness, i.e. 2 mm, and hence, these grooved coupons are under a plane strain state.
- 5) Grooved coupons with combined tension and shear stresses (NCIII-30): The geometry of the coupons in this series, i.e. Coupons NCIII-30, are identical to those grooved coupons described in Item 4) above. However, shear stresses are induced in these coupons by applying the tension action at an angle  $\theta$  to the plane of minimum cross-section, as shown in [Figure 6.2e](#)). When  $\theta$  equals to  $90^\circ$ , the loading direction is normal to the minimum cross-section, and the coupon is under tensile actions. However, with an decrease in  $\theta$ , the applied load will generate various combinations of tension and shear actions along the plane of minimum cross-section. Hence, there are three identical coupons, i.e. Coupons NCIII-30, and  $\theta = 30^\circ$ .
- 6) Notched coupons with combined tension and shear stresses (NCIV): Similar to the notched coupons with cylindrical cross-sections, these coupons are fabricated with the same rectangular cross-sections but different notch radii, i.e. 5.0, 2.5 and 1.25 mm, as shown in [Figure 6.2f](#)). It should be noted that the central portions of these coupons are designed with the shape of “a butterfly” [23]. Three rotation angles are adopted in these

coupons: 30°, 60° and 90°, and hence, they are designated as Coupons NCIV-30, NCIV-60 and NCIV-90.

The initial stress triaxiality and the Lode parameter of all these coupons are marked in **Figure 6.1**, and it is noted that they are all scattered in the first quadrant of the T-L space where these stress states are commonly encountered in practice.



**Figure 6.2** Configurations and dimensions of coupons. (Unit: mm)

### 6.3.2 Test programme

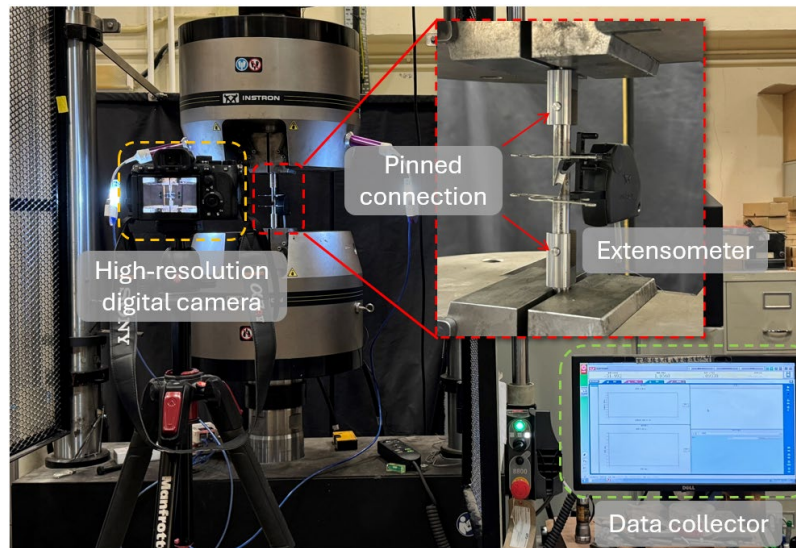
A total of 39 coupons were manufactured from the same S690 10 mm thick steel plate, as shown in **Table 6-2**. All the tensile tests on these coupons were conducted with the universal testing machine INSTRON 8803, and the test setup is illustrated in **Figure 6.3**. All the tests were conducted with a displacement control at a displacement rate of 0.05 mm/min. During tests, deformations of all these coupons were captured with a high-resolution digital camera while their extensions were measured with an extensometer having a 25 mm gauge



length. It should be noted that a pair of special clamps is designed for Coupons NCIII-30 and NCIV to realize the condition of “pinned supports” at both ends. Hence, the released in-plane rotations would ensure the central portions of these types of coupons were always under combined shear and tension actions.

**Table 6-2** Test programme.

| Actions              | Specimens                                                      | Parameters                  | No. of tests |
|----------------------|----------------------------------------------------------------|-----------------------------|--------------|
| Tension<br>action    | Cylindrical standard coupons<br>Coupon Standard                | 1 No. @ $R = 2.5$ mm        | 1            |
|                      | Notched coupons with cylindrical cross-section<br>Coupons NCI  | 3 No. @ $r = 1.25$ mm       | 9            |
|                      |                                                                | 3 No. @ $r = 2.5$ mm        |              |
|                      |                                                                | 3 No. @ $r = 5.0$ mm        |              |
|                      | Notched coupons with rectangular cross-section<br>Coupons NCII | 3 No. @ $r = 1.25$ mm       | 9            |
|                      |                                                                | 3 No. @ $r = 2.5$ mm        |              |
|                      |                                                                | 3 No. @ $r = 5.0$ mm        |              |
|                      | Grooved coupons<br>Coupons NCIII                               | 3 No. @ $r = 1.25$ mm       | 9            |
|                      |                                                                | 3 No. @ $r = 2.5$ mm        |              |
| 3 No. @ $r = 5.0$ mm |                                                                |                             |              |
| Shear<br>action      | Grooved coupons with combined stresses<br>Coupons NCIII-30     | 2 No. @ $\theta = 30^\circ$ | 2            |
|                      | Notched coupons with combined stresses<br>Coupons NCIV         | 3 No. @ $\theta = 30^\circ$ | 9            |
|                      |                                                                | 3 No. @ $\theta = 60^\circ$ |              |
|                      |                                                                | 3 No. @ $\theta = 90^\circ$ |              |
| Total No.            |                                                                |                             | 39           |



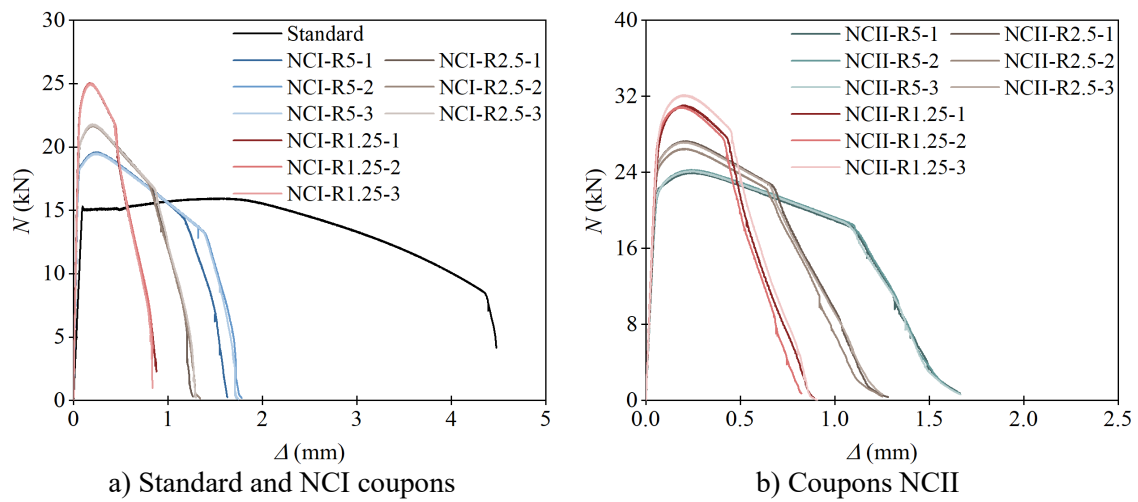
**Figure 6.3** Test setup for tensile tests.

### 6.3.3 Test results

All these tensile tests were conducted to fracture successfully, and typical fractured coupons after test are shown in [Figure 6.4](#). Measured load-extension ( $N - \Delta$ ) curves of various types of these notched coupons are plotted onto the graphs in [Figure 6.5](#) for easy comparison. It should be noted that the standard coupon is taken as a reference to provide basic mechanical properties of the S690 steel, which are listed in [Table 6-3](#). Its true stress-strain ( $\sigma_t - \varepsilon_t$ ) curve was calculated by the previously proposed Modified Bridgman's Method [81] in [Chapter 3](#), and the derived ( $\sigma_t - \varepsilon_t$ ) curve is plotted in [Figure 6.6](#). For coupons NCI, NCII and NCIII, the maximum applied loads of these coupons are found to have increased, and yet, their extension limits are found to be decreased with a decrease in their notched radii. For those Coupons NCIV, their deformation characteristics are shown to differ significantly when the rotation angle  $\theta$  varies from 30 to 90°. With an increase in the value of  $\theta$ , the maximum applied loads of the coupons are found to increase together with significant decreases in their extension limits.



Figure 6.4 Fractured coupons.



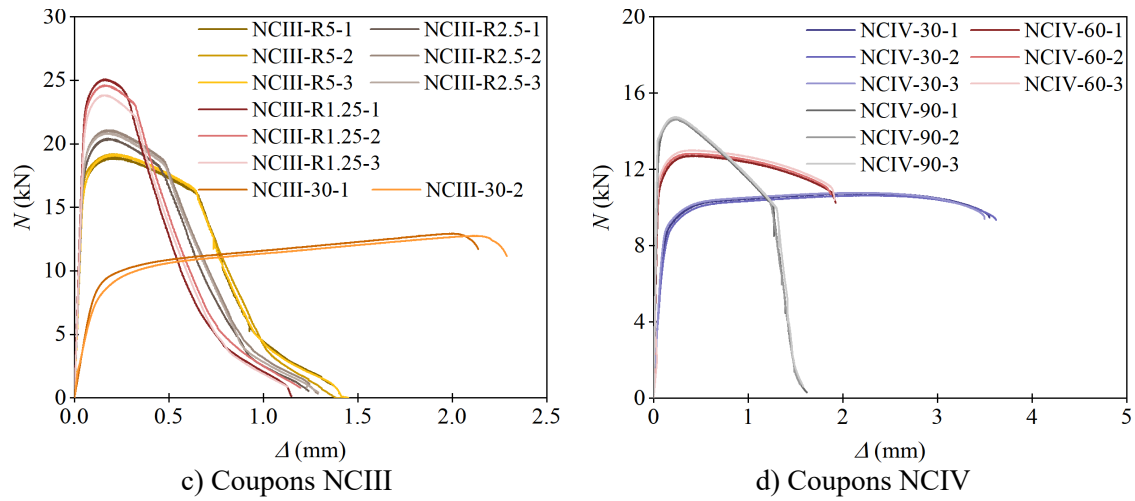


Figure 6.5 Load-extension ( $N$ - $\Delta$ ) curves of all tested coupons.

Table 6-3 Basic mechanical properties.

| Material | Young's modulus<br>$E$<br>(kN/mm <sup>2</sup> ) | Yield strength<br>$f_y$<br>(N/mm <sup>2</sup> ) | Tensile strength<br>$f_u$<br>(N/mm <sup>2</sup> ) | Strain at $f_u$<br>$\varepsilon_n$<br>(%) | Strain at fracture<br>$\varepsilon_L$<br>(%) |
|----------|-------------------------------------------------|-------------------------------------------------|---------------------------------------------------|-------------------------------------------|----------------------------------------------|
| S690     | 209                                             | 775                                             | 825                                               | 6.1                                       | 17.9                                         |

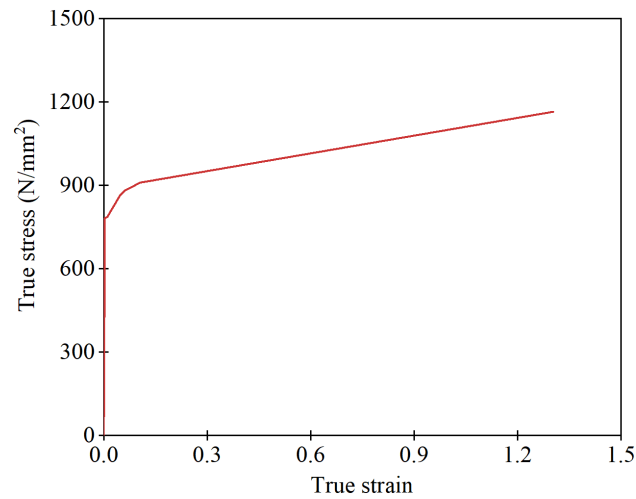


Figure 6.6 True stress-strain ( $\sigma_t$  -  $\varepsilon_t$ ) curve of S690 steel.

### 6.3.4 Microscopic observations using SEM

The morphology of fracture surfaces of typical fractured coupons was observed under scanning electron microscopes. Several coupons with similar stress triaxiality or Lode parameters at fracture initiation point were selected for comparison, and their stress states are plotted in  $T_{av} - L_{av}$  space, as shown in [Figure 6.7](#), while their fracture surfaces at the same magnification (500 x) are shown [Figure 6.8](#). It should be noted that determination of both  $T_{av}$  and  $L_{av}$  should refer to [Chapter 6.4](#). The coupons with controlled notched radii are selected, i.e. Coupons NCI-R2.5, NCII-R2.5 and NCIII-R2.5. They exhibit comparable stress triaxiality at fracture initiation point as they are all in the high stress triaxiality region ( $T_{av} \geq 2/3$ ). Meanwhile, Coupons NCIV-30 and NCIV-90 have comparable Lode parameters with Coupon NCII-R2.5 in the medium Lode parameter region ( $-1/3 \leq L_{av} < -2/3$ ), and Coupon NCIII-30 has a similar small Lode parameter ( $0 \leq L_{av} < -1/3$ ) with Coupon NCIII-R2.5. Two failure modes have been observed among all these coupons, which are i) shear failure with a smooth surface, i.e. Coupon NCIII-30-2 in [Figure 6.8a](#)), and ii) tensile failure with dimples, i.e. Coupon NCI-R2.5-1 in [Figure 6.8g](#)). It should be noted that:

- Under high stress triaxiality conditions ( $T_{av} \geq 2/3$ ), fracture surfaces of Coupons NCIII-R2.5-3, NCII-R2.5-1 and NCI-R2.5-1, as shown in [Figure 6.8b](#)), [e](#)) and [g](#)), display valley-and-plateau features characterized by significant roughness. Coupons with elevated Lode parameters within this regime exhibit apparent dimple structures, which are driven by large deformations at microstructural levels.
- In medium stress triaxiality regions ( $1/3 \leq T_{av} < 2/3$ ), a transitional morphology emerged in Coupons NCIII-30-2, NCIV-90-1 and Standard Coupons, as shown in

Figure 6.8a), d) and f), where reduced Lode parameters result in a smoother fracture surface. Conversely, larger Lode parameters lead to a rough fracture surface with smaller dimples, compared to those coupons in large stress triaxiality.

- Coupons in small stress triaxiality ( $0 \leq T_{av} < 1/3$ ), i.e. Coupon NCIV-60-1, as shown in Figure 6.8c), present consistently smooth fracture surfaces across all Lode parameter values, suggesting shear-dominated failure modes.

The morphology of a fracture surface of fracture coupons demonstrates a dependence on both stress triaxiality and Lode parameter, revealing distinct failure mechanisms across different stress states. Notably, under conditions of large stress triaxiality and Lode parameters, characteristic dimple patterns emerge on the fracture surface. Conversely, when Lode parameters decrease while maintaining comparable triaxiality levels, this rough morphology formed by dimples transitions to smoother fracture surfaces. This transition becomes particularly apparent in small triaxiality regions, where shear stress dominates the fracture occurrence, leading to a smooth fracture surface through shear localization processes. These observations establish fundamental relationships between multi-axial loading conditions and their corresponding failure modes, specifically highlighting the competitive interaction between microstructural deformations controlled by the stress triaxiality and a smooth surface driven by the shear stresses.

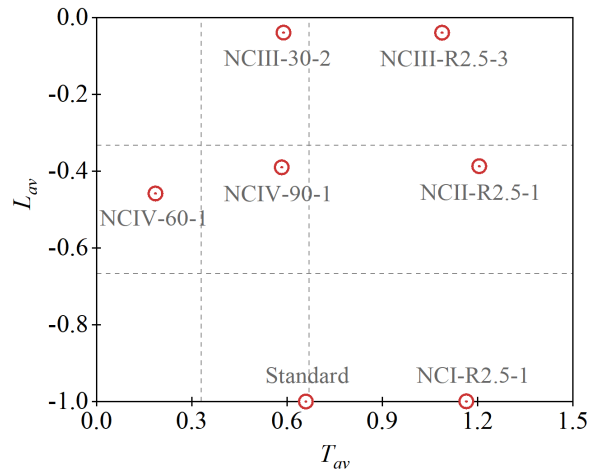


Figure 6.7 Critical stress states of fractured coupons.

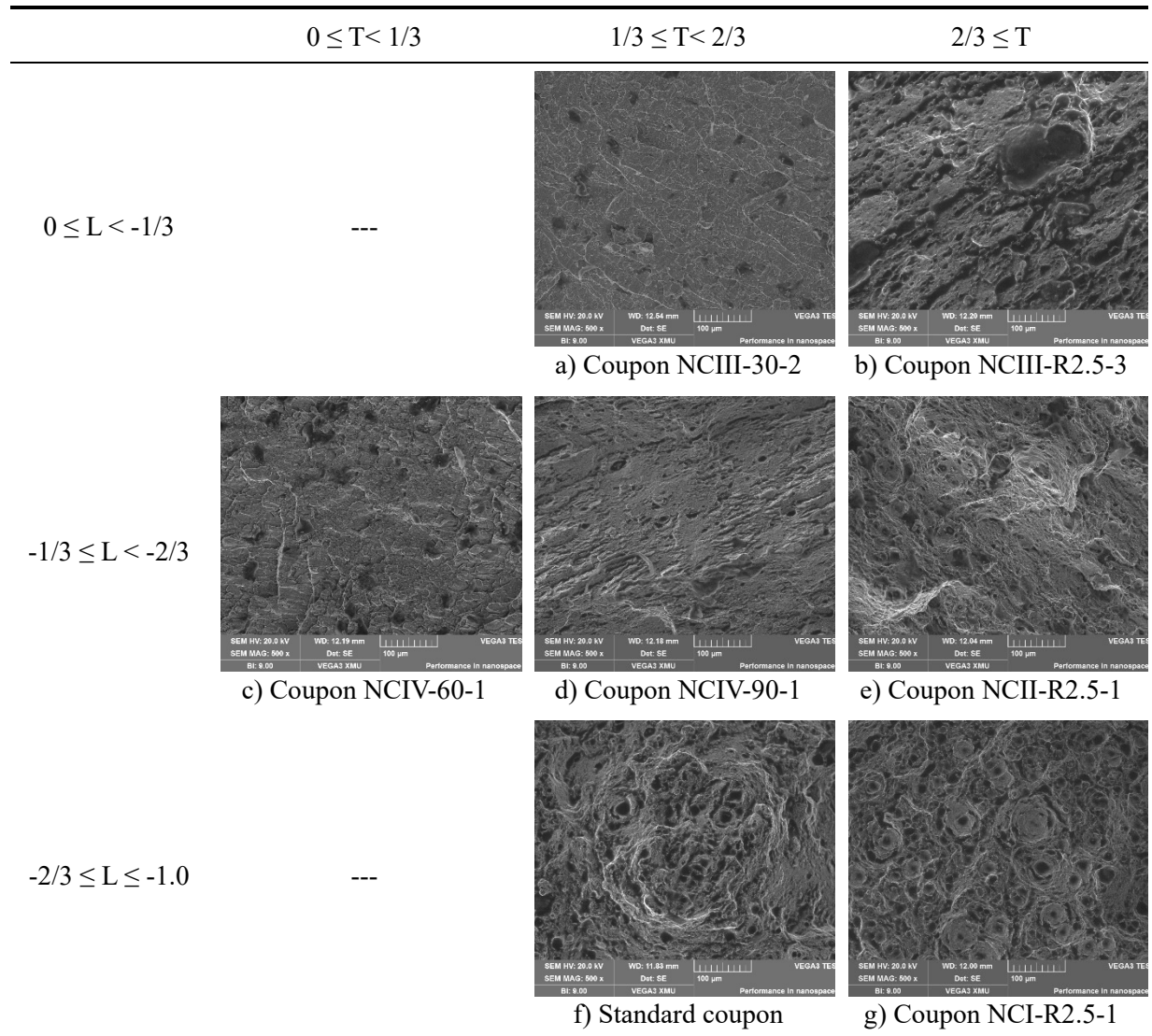


Figure 6.8 Microstructure of fracture surface.



## 6.4 Calibration of ductile fracture model

### 6.4.1 Numerical simulation

Numerical models were developed with ABAQUS 2020 [85] using measured geometrical dimensions of the tested coupons. As shown in [Figure 6.9](#), the models were meshed with eight-noded linear hexahedral solid elements, i.e. Element C3D8R. This element was selected to attain a balance between computational efficiency and numerical accuracy. An effective meshing strategy was implemented, using a global mesh size of 2.0 ~ 5.0 mm and a refined local mesh of 0.10 ~ 0.25 mm near the mid-lengths of these models. This meshing strategy was applied consistently to all models to resolve high strain gradients effectively while maintaining computational economy. It should be noted that reliability and convergence of this meshing strategy has been validated in previous studies by the authors and other researchers [62, 80].

Different boundary conditions were provided to these models to replicate various conditions imposed onto the coupons in tests, as shown in [Figure 6.9](#). For most specimens, the bottom ends of the models were fully constrained to represent clamping while a longitudinal displacement ( $\Delta$ ) was applied uniformly to all the nodes at the top end via constraint equations. To reduce computational resources, symmetry modelling was adopted where appropriate: 1/8 models were used for the Standard Coupon and Coupons NCI, NCII, and NCIII, and 1/2 models were employed for Coupons NCIII-30 and NCIV. It should also be noted that a pinned-pinned condition at both ends of Coupons NCIII-30 and NCIV was adopted according to the loading conditions in tests, and hence, these models were precisely aligned to ensure a correct load transfer across the inclined notched sections. In all these models of the coupons, the constitutive model of the steel was derived from the



modified true stress-strain ( $\sigma_t - \varepsilon_t$ ) curve plotted in Figure 6.6.

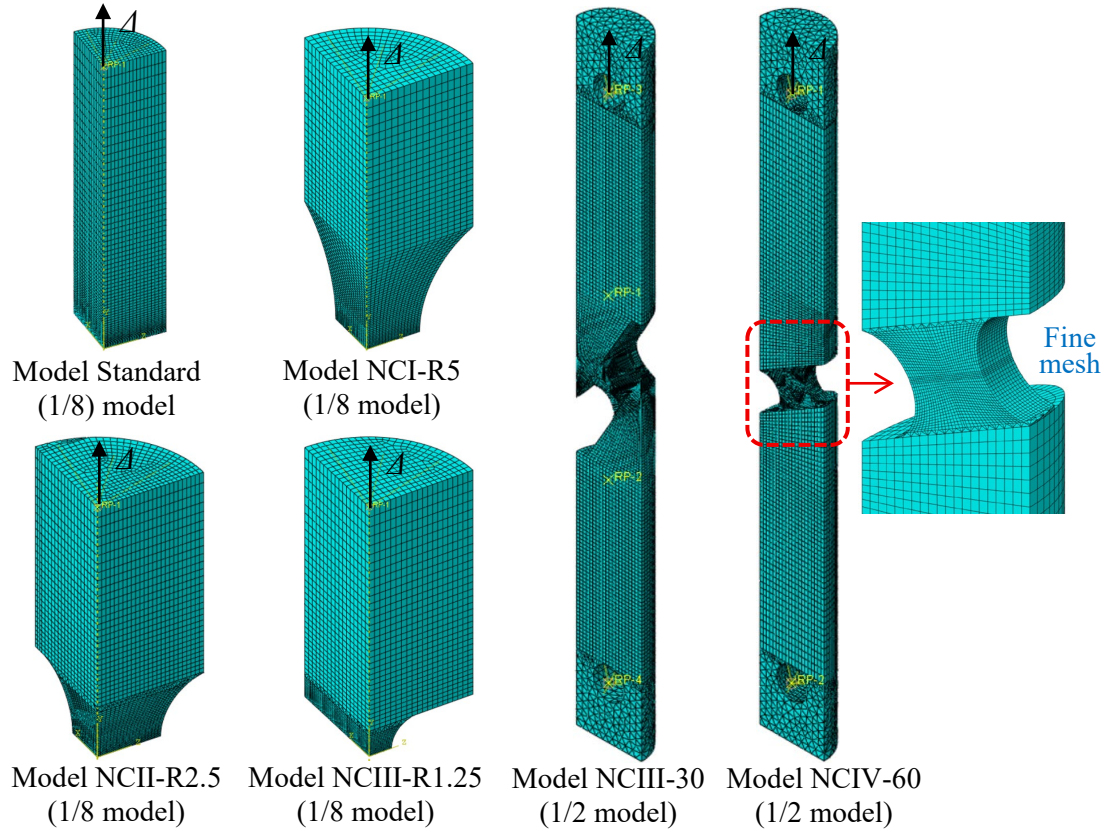


Figure 6.9 Boundary conditions and typical meshes of various models.

Deformed shapes of Coupon NCIII-30 at various extension stages are illustrated in Figure 6.10 together with a distribution of its equivalent plastic strains PEEQ. Figure 6.11 presents both the predicted and the measured load-extension ( $N - \Delta$ ) curves for different coupons. The results show a strong correlation between the predicted and the measured values across both the linear and the nonlinear deformation regions. The deformation at the onset of fracture for each coupon was identified according to the test data. Then, the corresponding numerical results, including the critical states of stress and strain at fracture, were also determined. As a result, the critical states of stress and strain at fracture initiation for all 39 coupons were directly extracted from the numerical models for further analysis.

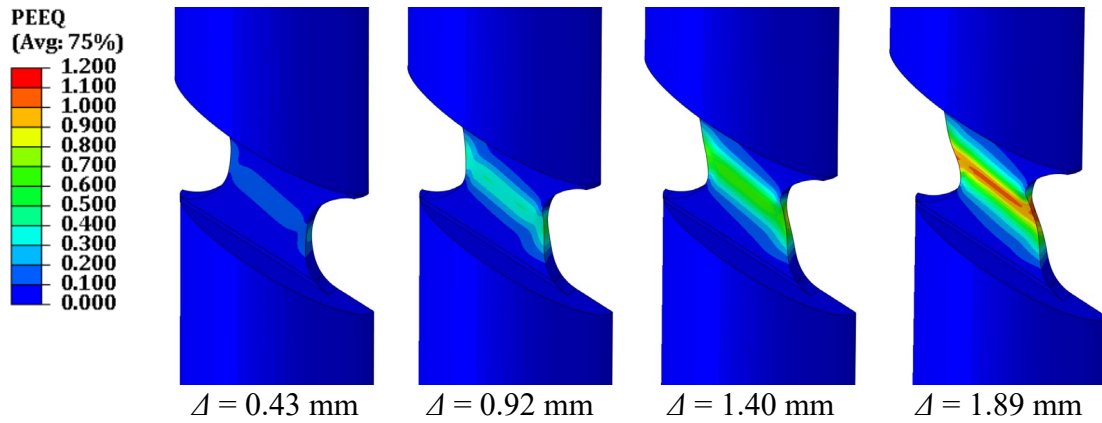


Figure 6.10 Deformed shapes of Model NCIV-60 at different extensions.

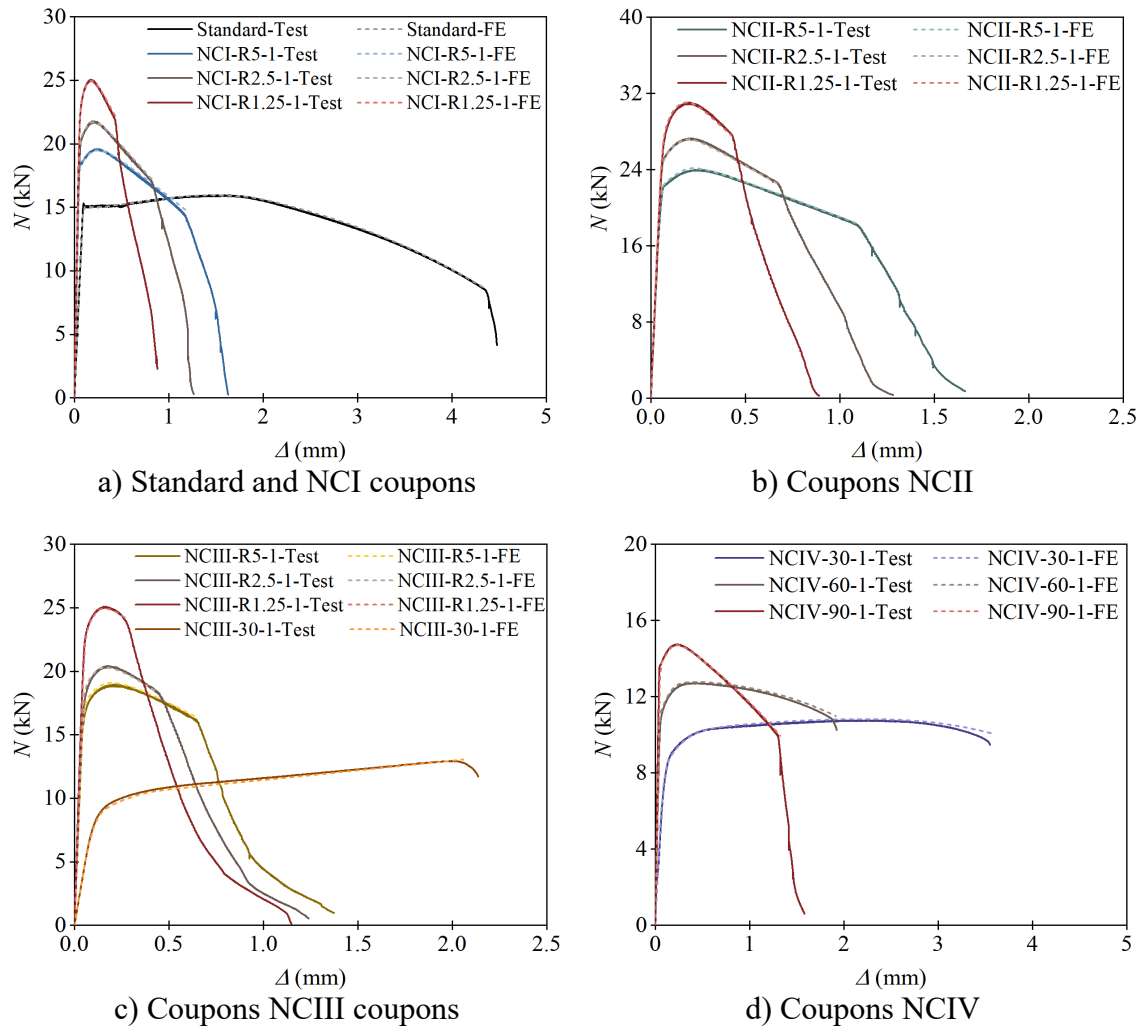


Figure 6.11 Comparison of predicted and measured load-extension ( $N$ - $\Delta$ ) curves

Then, the states of stress and strain at the fracture initiation positions are obtained from the numerical models. It is observed from the tests that Standard coupons and Coupons NCI, NCII and NCIII are fractured from the centre of their minimum cross-sections while fracture of Coupons NCIII-30 and NCIV are initiated from the centre of the surfaces of their critical parts. The fracture strain  $\varepsilon_f$  is defined as the equivalent plastic strain at fracture deformation  $\Delta f$ . The average stress triaxiality  $T_{av}$  and the average Lode parameter  $L_{av}$  are calculated from the stress state evolution through Eq. 5-2 and Eq. 5-3. All the stress states at fracture ( $T_{av, f}$ ,  $L_{av, f}$ ) are summarized in Table 6-4. It is noted that, up to fracture, the stress triaxiality varies typically from 0.1 to 1.6, and the Lode parameter ranges from 0 to -1. Besides, at the similar Lode parameter level, the fracture strain decreases with an increase in the stress triaxiality. Both the notch radius and the rotation angle are found to affect the values of stress triaxiality in those coupons with small notch radii while larger rotation angles give larger stress triaxiality. Meanwhile, the value of the Lode parameter is heavily influenced by the rotation angle. In Coupons NCIII-30 and NCIV, and a decrease in the rotation angle introduces larger shear stresses to the critical parts, which leads to a larger difference between the principal stresses. Thus, a larger denominator is found in Eq. 2-14 which decreases significantly the value of Lode parameter.

**Table 6-4** Critical states of stress and strain at fracture initiation.

| Coupons              | $\varepsilon_f$ | $T_{av, f}$ | $L_{av, f}$ | $\Delta_f$ | $\Delta_{f, predict}$ | Error (%) |
|----------------------|-----------------|-------------|-------------|------------|-----------------------|-----------|
| Standard             | 1.301           | 0.659       | -1.000      | 4.323      | 4.122                 | 4.7       |
| NCI-R5-1             | 0.725           | 0.941       | -1.000      | 1.345      | 1.282                 | 4.7       |
| NCI-R5-2             | 0.788           | 0.946       | -1.000      | 1.361      | 1.317                 | 3.2       |
| NCI-R5-3             | 0.775           | 0.945       | -1.000      | 1.374      | 1.335                 | 2.8       |
| NCI-R2.5-1           | 0.493           | 1.165       | -1.000      | 0.798      | 0.819                 | 2.7       |
| NCI-R2.5-2           | 0.483           | 1.164       | -1.000      | 0.787      | 0.819                 | 4.1       |
| NCI-R2.5-3           | 0.534           | 1.166       | -1.000      | 0.843      | 0.819                 | 2.8       |
| NCI-R1.25-1          | 0.227           | 1.493       | -1.000      | 0.416      | 0.479                 | 15.2      |
| NCI-R1.25-2          | 0.242           | 1.496       | -1.000      | 0.425      | 0.479                 | 12.6      |
| NCI-R1.25-3          | 0.235           | 1.495       | -1.000      | 0.440      | 0.479                 | 8.7       |
| <i>Average value</i> |                 |             |             |            |                       | 6.2       |
| NCII-R5-1            | 0.462           | 0.978       | -0.501      | 1.079      | 0.930                 | 13.8      |
| NCII-R5-2            | 0.474           | 0.980       | -0.500      | 1.096      | 0.930                 | 15.2      |
| NCII-R5-3            | 0.450           | 0.977       | -0.502      | 1.062      | 0.930                 | 12.5      |
| NCII-R2.5-1          | 0.338           | 1.205       | -0.387      | 0.654      | 0.602                 | 8.0       |
| NCII-R2.5-2          | 0.303           | 1.200       | -0.394      | 0.608      | 0.576                 | 5.3       |
| NCII-R2.5-3          | 0.315           | 1.202       | -0.391      | 0.625      | 0.602                 | 3.7       |
| NCII-R1.25-1         | 0.197           | 1.535       | -0.350      | 0.398      | 0.414                 | 3.9       |
| NCII-R1.25-2         | 0.186           | 1.531       | -0.359      | 0.387      | 0.414                 | 7.0       |
| NCII-R1.25-3         | 0.230           | 1.545       | -0.329      | 0.421      | 0.459                 | 9.1       |
| <i>Average value</i> |                 |             |             |            |                       | 8.7       |
| NCIII-R5-1           | 0.716           | 0.871       | -0.030      | 0.632      | 0.576                 | 8.8       |
| NCIII-R5-2           | 0.695           | 0.868       | -0.032      | 0.614      | 0.576                 | 6.1       |
| NCIII-R5-3           | 0.673           | 0.865       | -0.033      | 0.613      | 0.576                 | 6.1       |
| NCIII-R2.5-1         | 0.567           | 1.082       | -0.038      | 0.433      | 0.416                 | 3.9       |
| NCIII-R2.5-2         | 0.584           | 1.091       | -0.036      | 0.451      | 0.428                 | 5.0       |
| NCIII-R2.5-3         | 0.595           | 1.089       | -0.039      | 0.455      | 0.441                 | 3.1       |
| NCIII-R1.25-1        | 0.395           | 1.342       | -0.067      | 0.258      | 0.269                 | 4.3       |
| NCIII-R1.25-2        | 0.434           | 1.337       | -0.058      | 0.306      | 0.291                 | 4.7       |
| NCIII-R1.25-3        | 0.481           | 1.323       | -0.045      | 0.314      | 0.302                 | 3.8       |
| <i>Average value</i> |                 |             |             |            |                       | 5.1       |
| NCIII-30-1           | 0.753           | 0.589       | -0.039      | 1.959      | 1.951                 | 0.4       |
| NCIII-30-2           | 0.848           | 0.586       | -0.036      | 2.206      | 2.244                 | 1.7       |
| <i>Average value</i> |                 |             |             |            |                       | 1.1       |
| NCIV-30-1            | 1.367           | 0.111       | -0.288      | 3.490      | 3.299                 | 5.5       |
| NCIV-30-2            | 1.407           | 0.114       | -0.297      | 3.617      | 3.299                 | 8.8       |
| NCIV-30-3            | 1.308           | 0.106       | -0.276      | 3.465      | 3.337                 | 3.7       |
| NCIV-60-1            | 0.991           | 0.186       | -0.458      | 1.890      | 1.952                 | 3.3       |
| NCIV-60-2            | 0.962           | 0.183       | -0.452      | 1.845      | 1.911                 | 3.6       |
| NCIV-60-3            | 1.003           | 0.186       | -0.457      | 1.895      | 1.942                 | 2.5       |
| NCIV-90-1            | 0.651           | 0.583       | -0.390      | 1.273      | 1.341                 | 5.3       |
| NCIV-90-2            | 0.618           | 0.581       | -0.395      | 1.239      | 1.341                 | 8.2       |
| NCIV-90-3            | 0.685           | 0.586       | -0.384      | 1.307      | 1.341                 | 2.6       |
| <i>Average value</i> |                 |             |             |            |                       | 4.8       |

## 6.4.2 Calibration of parameters

A new ductile fracture model was proposed in Chapter 5 which was based on a comprehensive analysis and interpretation on the test data. Firstly, the proposed model works well within the range of stress states commonly encountered in practical situations, which is in the region of first quadrant in Figure 6.1. Then, the axial tension state and the plane strain state are defined as two boundaries of three-dimensional fracture loci. It is found that the effect of stress triaxiality is well described with a classical exponential function while the effect of the Lode parameter may be readily described in a quadratic format. Hence, the expression of the ductile fracture model is given by:

$$\varepsilon_f = C_1 e^{-C_2 T_{av}} \cdot L_{av}^2 + C_3 e^{-C_4 T_{av}} \cdot L_{av} + C_3 e^{-C_4 T_{av}} \quad (6-1)$$

where  $C_1$  to  $C_4$  are four free parameters. These parameters have been calibrated against test data as shown in Table 6-2, and they are summarized in Table 6-5. The fracture loci generated by Eq. 6-1 are plotted in the  $T_{av} - L_{av} - \varepsilon_f$  space, as shown in Figure 6.12.

Table 6-5 Calibrated parameters in the proposed ductile fracture model.

| Parameters | $C_1$ | $C_2$ | $C_3$ | $C_4$ |
|------------|-------|-------|-------|-------|
| Value      | 2.82  | 1.45  | 1.59  | 1.0   |

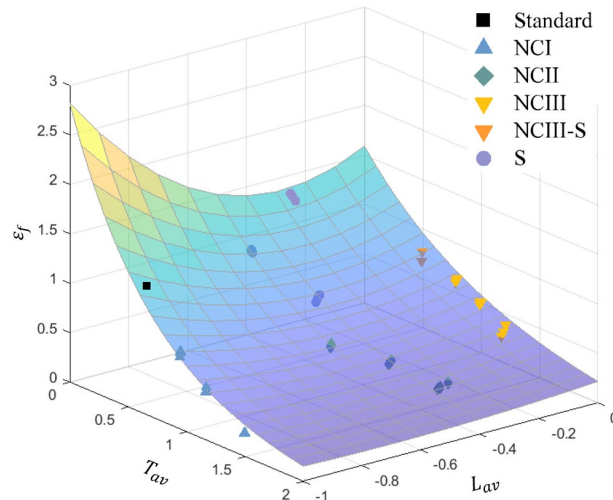


Figure 6.12 Fracture loci of the proposed ductile fracture model. S690 steel.

## 6.5 Experiments on bolted connections

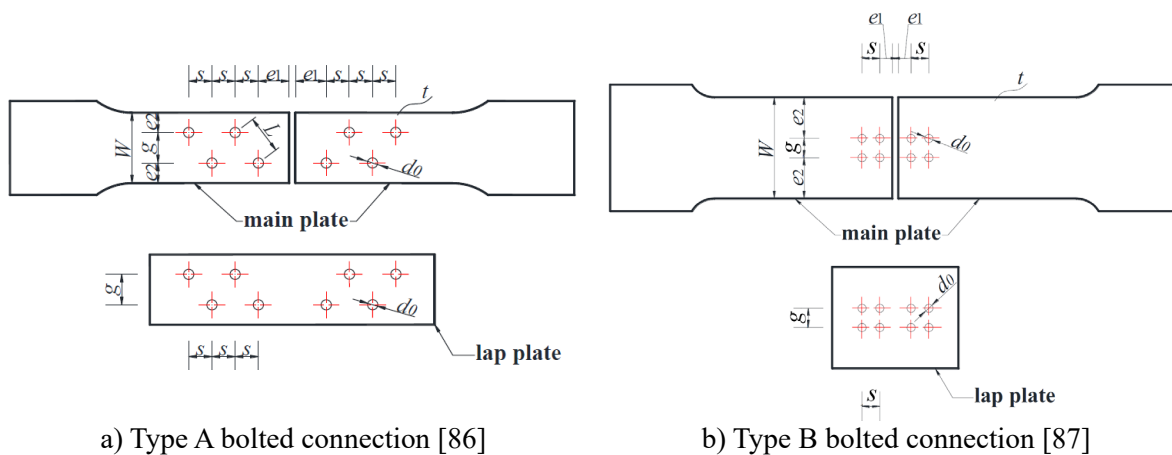
After calibration, the proposed ductile fracture model is employed to predict the ductile fracture behaviour of S690 bolted connections, and a set of relevant test data is collected from the study of Lin et.al [86, 87].

### 6.5.1 Design of specimens

Two bolted connections with different configurations are covered in the test programme:

- i) Type A bolted connection - Staggered bolted connections with a potential net section failure, and
- ii) Type B bolted connection - Regular bolted connections with a potential block shear failure.

Typical geometry of these two types of bolted connections is illustrated in **Figure 6.13**, and their key dimensions are summarized in **Table 6-6**.



**Figure 6.13** Configurations of bolted connections.

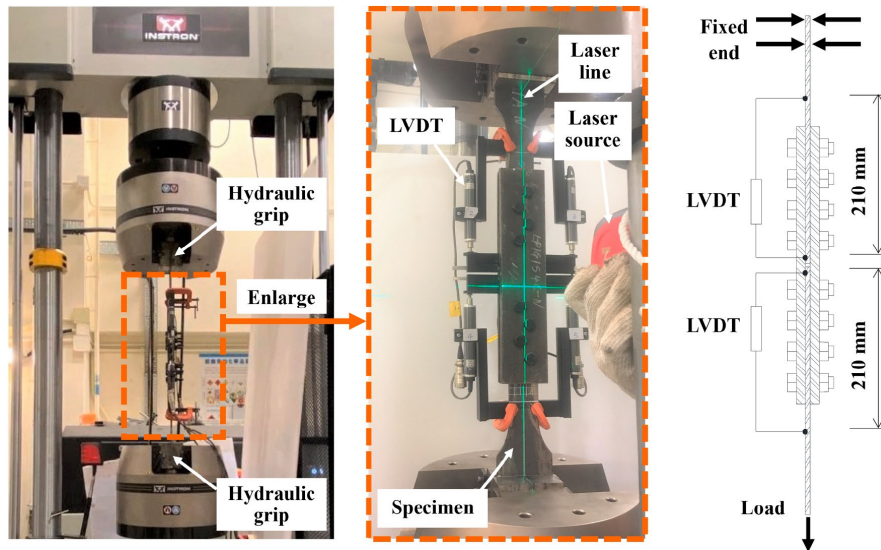
**Table 6-6** Measured dimensions of test specimens.

| Bolted connections |             | $W$<br>(mm) | $t$<br>(mm) | $d_0$<br>(mm) | $s$<br>(mm) | $g$<br>(mm) | $s/g$ | $e_1$<br>(mm) | $e_2$<br>(mm) |
|--------------------|-------------|-------------|-------------|---------------|-------------|-------------|-------|---------------|---------------|
| Type A             | P1G40S32    | 72.8        | 5.8         | 13.1          | 31.8        | 39.9        | 0.80  | 40            | 16.5          |
|                    | P1G30S22    | 62.7        | 5.8         | 13.1          | 22.0        | 30.8        | 0.67  | 40            | 16.0          |
|                    | P1G30S15    | 62.6        | 5.8         | 13.1          | 15.2        | 30.3        | 0.50  | 40            | 16.2          |
|                    | P1G40S15    | 72.7        | 5.9         | 13.1          | 14.9        | 40.5        | 0.38  | 40            | 16.1          |
| Type B             | P1-22-34-26 | 116.1       | 5.9         | 10.7          | 34.6        | 25.4        | 1.36  | 22.2          | 45.4          |
|                    | P1-22-24-44 | 134.0       | 5.8         | 10.7          | 24.7        | 43.9        | 0.55  | 22.2          | 45.1          |

### 6.5.2 Test programme

These bolted connections were tested under monotonic tension in a universal testing machine, INSTRON 8803, and the test setup is illustrated in [Figure 6.14](#). Two linear variable differential transformers (LVDTs) are installed on both sides of the main plates of the connections to measure extensions of the bolted connections between two specific points, and they are 210 and 170 mm, respectively for Types A and B bolted connections.

Detailed information may be found in References [86, 87].



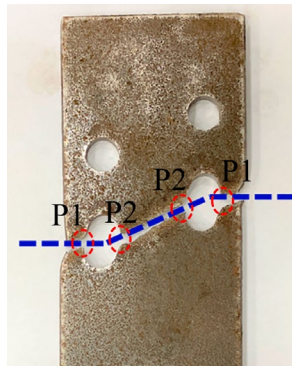
**Figure 6.14** Test setup for bolted connections under tension [86].



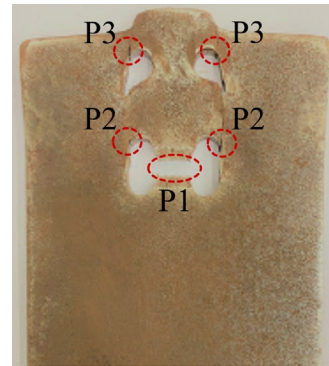
### 6.5.3 Test results

Typical failure modes of Types A and B bolted connections are illustrated in **Figure 6.15**, and their main plates are found to have fractured in different ways as follows:

- For those Type A bolted connections, fracture is found to have initiated from two hole edges of the main plates, marked as Points P1 in **Figure 6.15a**). Then, cracks are found to have propagated to other sides of the bolt holes, marked as Points P2 also in **Figure 6.15a**), under further loading. Eventually, fracture across the entire net sections of the main plates take place.
- For those Type B bolted connections, a tension fracture is first observed between two transverse holes, marked as Point P1 in **Figure 6.15b**). After that, cracks are observed to propagate to the outside edges of two other holes, marked as Points P2 in **Figure 6.15b**). Subsequently, fracture is fully developed at similar positions at the other two holes, marked as Points P3 in **Figure 6.15b**).



a) Type A bolted connection, P1G40S15 [86]

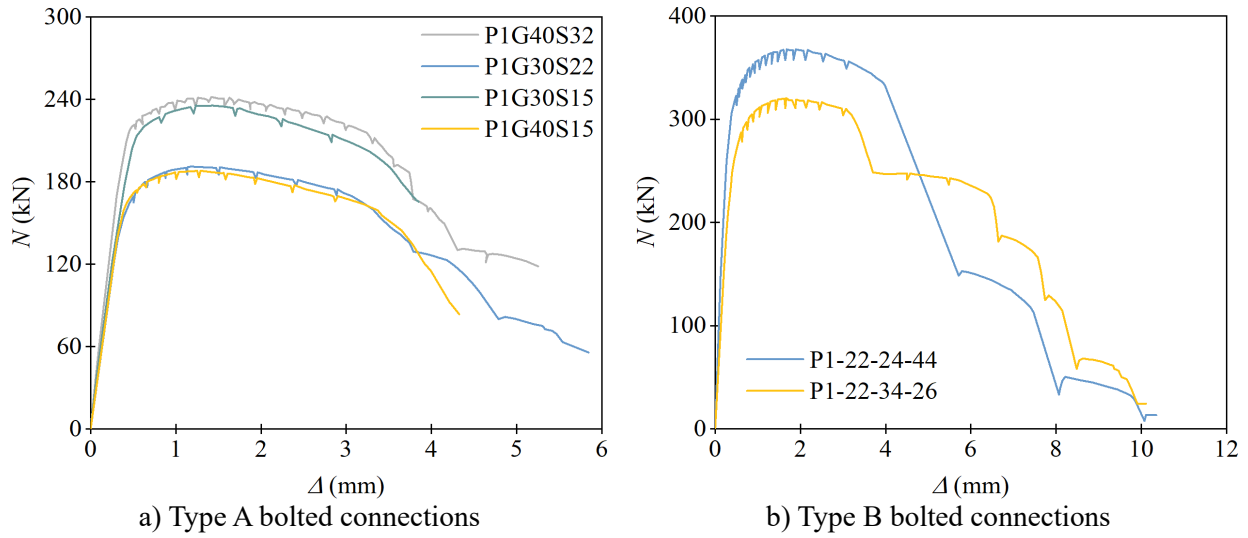


b) Type B bolted connection, P1-22-34-26 [87]

**Figure 6.15** Typical failure modes in bolted connections.

All the load-extension curves of these bolted connections are plotted in **Figure 6.16**.





**Figure 6.16** Load-extension ( $N-\Delta$ ) curves of bolted connections.

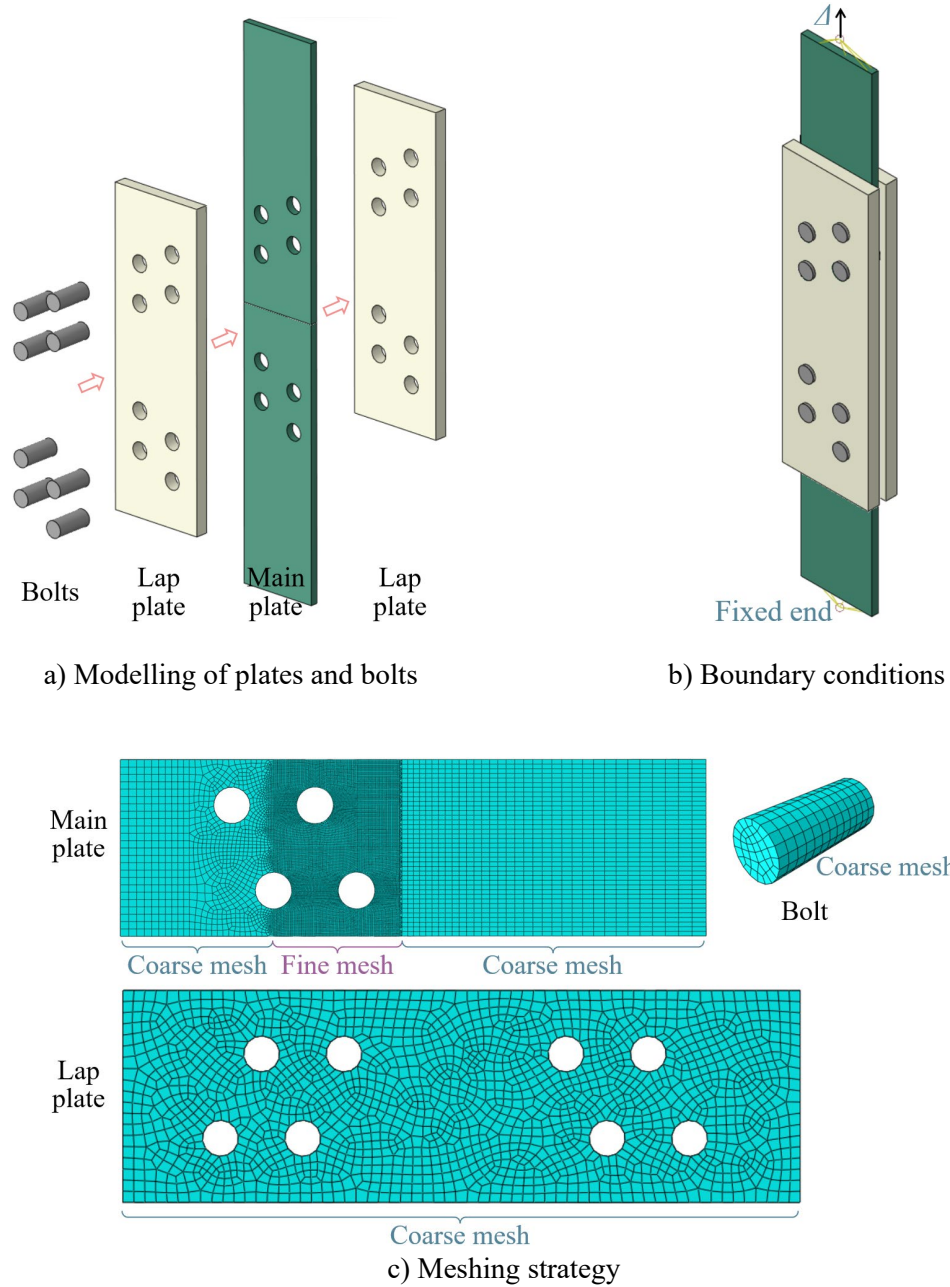
#### 6.5.4 Numerical simulation

These bolted connections were analyzed using ABAQUS/Explicit, and the numerical models of Type A and Type B bolted connections are illustrated in **Figures 6.17** and **6.18** respectively. Hexahedral reduced-integration elements (C3D8R) were employed. For effective modelling, the mesh sizes were designed to vary from 0.1 to 4.0 mm as follows:

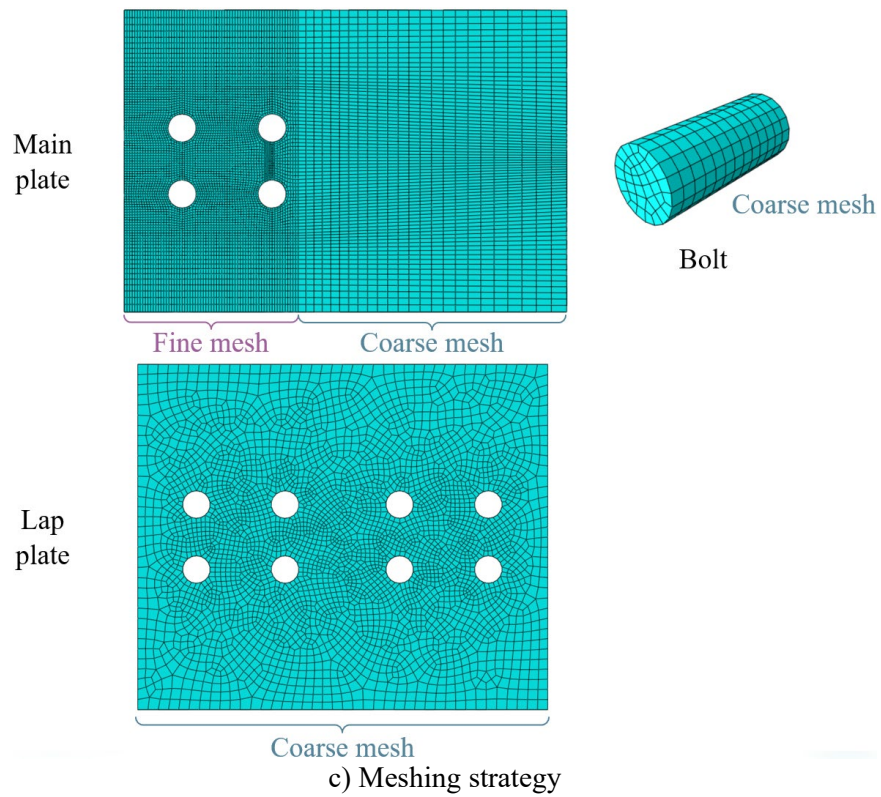
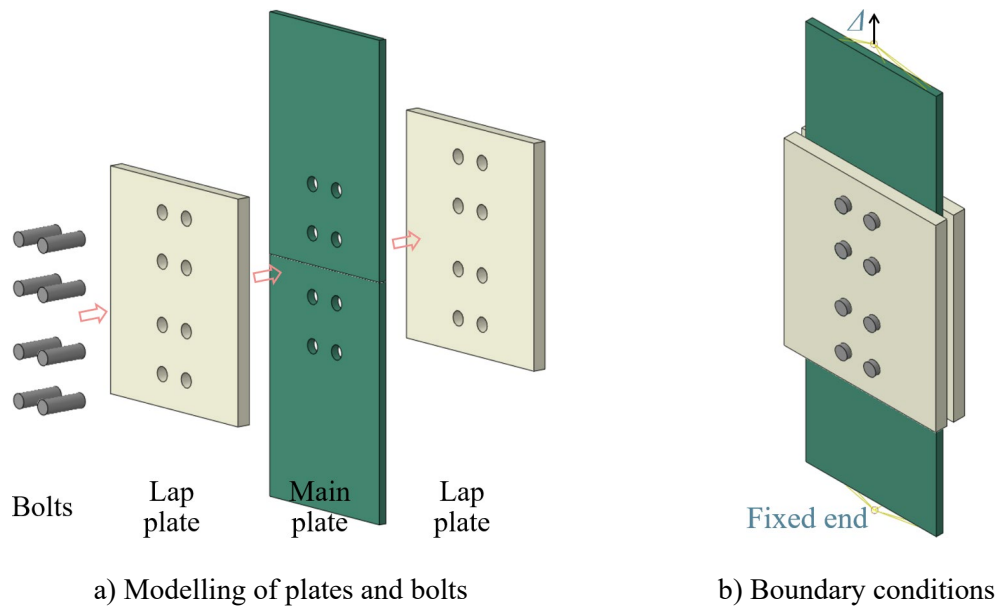
- in general, the mesh sizes of the lap plates and the bolts are assigned to be 4.0 and 2.0 mm respectively, and
- the mesh sizes of the main plates in the vicinity of the bolt holes are assigned to vary from 0.1 to 0.3 mm, depending on specific local configurations.

A fixed boundary was applied to one end of the model while a quasi-static displacement along the longitudinal direction was applied to the other end of the specimens. A "hard" normal behaviour was employed together with a tangential friction coefficient of 0.2 in contact interactions according to EN 1993-1-8 Class D. Three true stress-strain curves for different steel plates are plotted in **Figure 6.19**, and they are developed according to the test

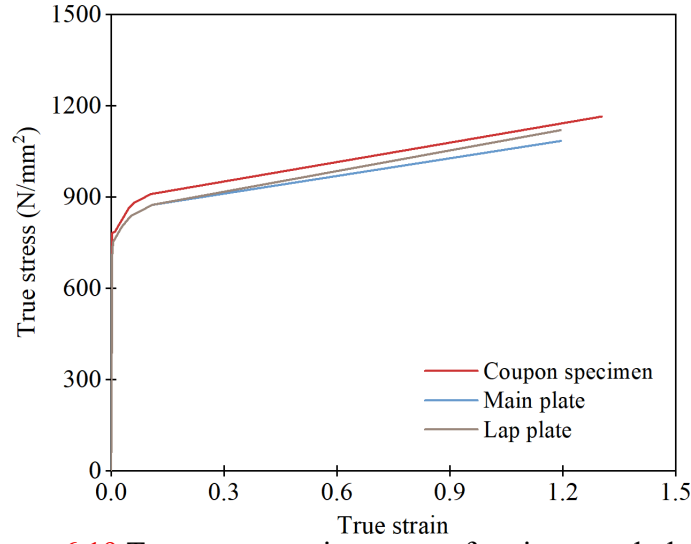
results of the tensile tests reported in **Chapter 3**.



**Figure 6.17** Finite element model of Type A bolted connection. P1G30S15.



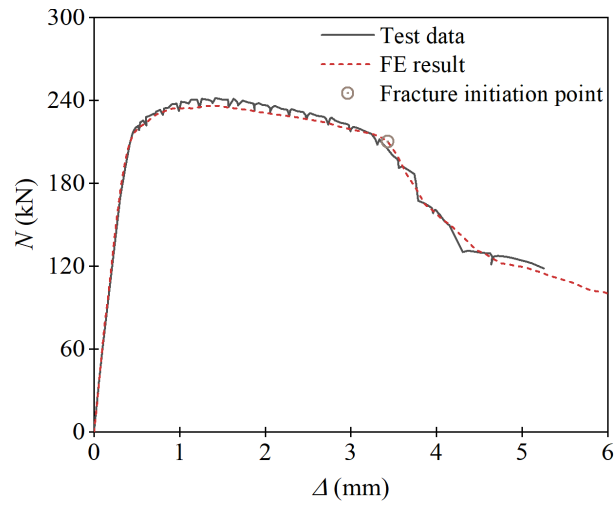
**Figure 6.18** Finite element model of Type B bolted connection. P1-22-34-26.



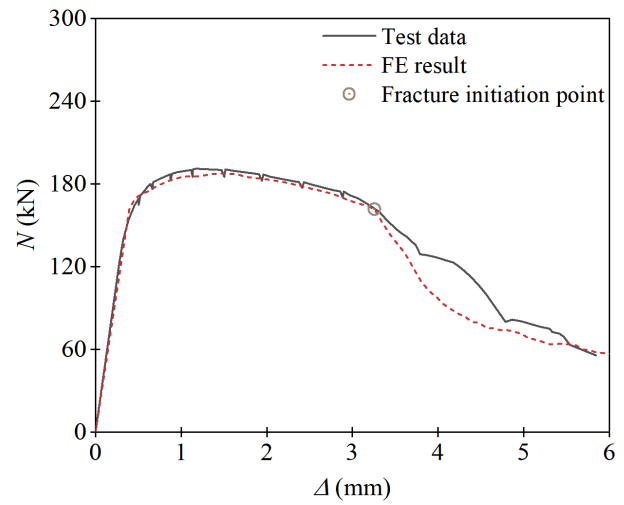
**Figure 6.19** True stress-strain curves of various steel plates.

### 6.5.5 Ductile fracture prediction

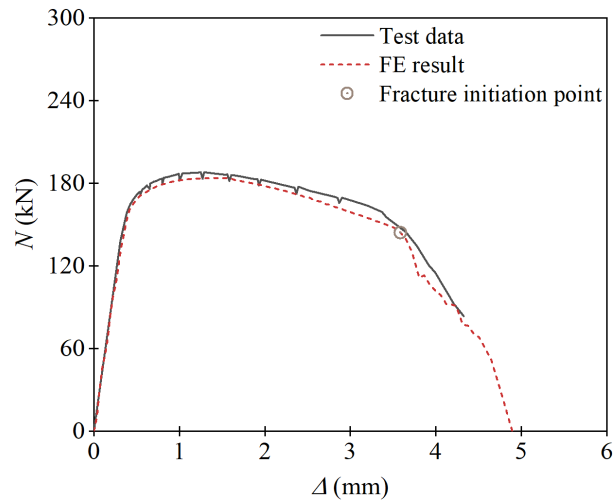
After establishing the numerical models, fracture behaviour of the bolted connections is predicted using the calibrated ductile fracture model. In ABAQUS/Explicit analysis, a user subroutine VUSDFLD was defined to calculate the average stress triaxiality  $T_{av}$  and average Lode parameter  $L_{av}$  at each integration point of solid elements in the vicinity of bolt holes of the main plates. The evolution of  $T_{av}$  and  $L_{av}$  near bolt holes were calculated from numerical data, and then substituted into the proposed ductile fracture model to determine various conditions of fracture initiation. **It is important to note that fracture initiation was assessed with a fracture strain  $\epsilon_f$  which was specifically calculated in the fracture loci of the proposed fracture model, i.e. the  $T_{av} - L_{av} - \epsilon_f$  space, as shown in Figure 6.12.** Fracture was considered to occur when the equivalent plastic strain  $\epsilon_{eq}$  of elements reached  $\epsilon_f$ , and then, these elements were deleted from the model to simulate crack propagation of the bolted connections before subsequent analyses. The predicted load-extension curves of bolted connections are plotted in Figure 6.20.



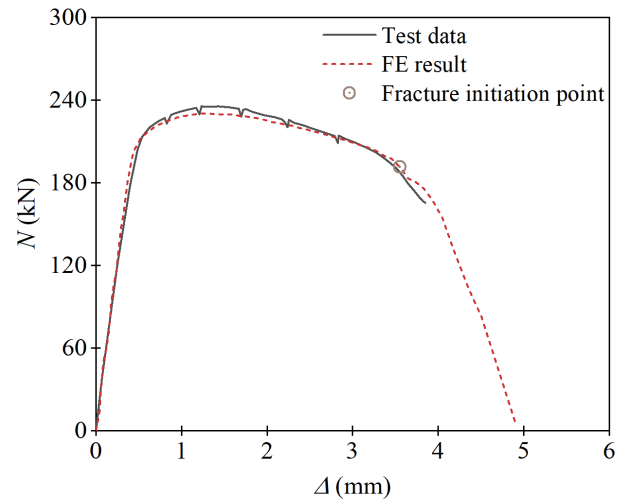
a) Type A bolted connection - P1G40S32



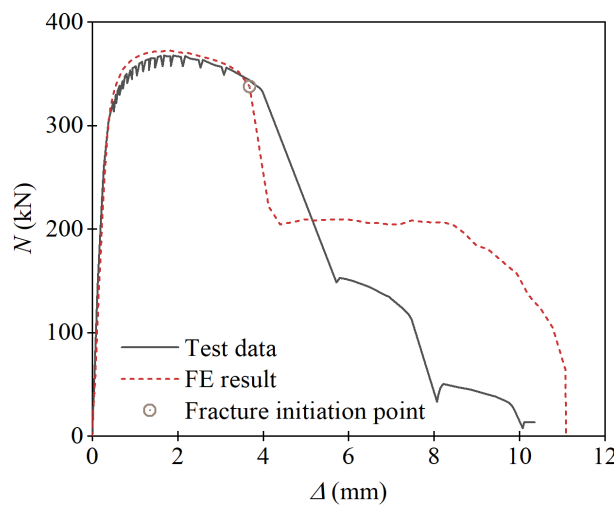
b) Type A bolted connection - P1G30S22



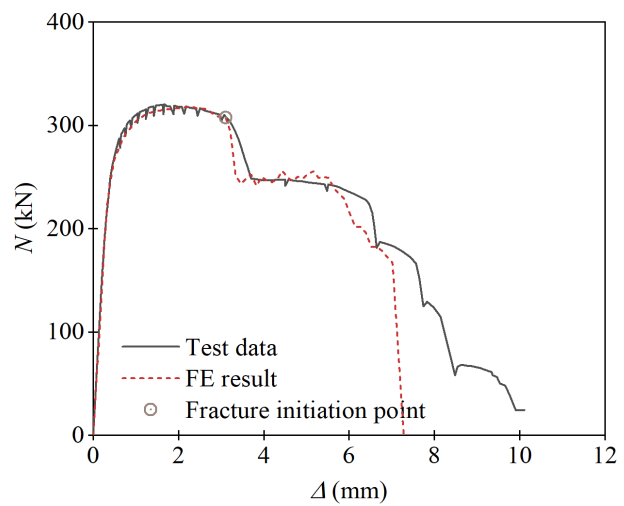
c) Type A bolted connection - P1G30S15



d) Type A bolted connection - P1G40S15



e) Type B bolted connection - P1-22-24-44



f) Type B bolted connection - P1-22-34-26

**Figure 6.20** Load-extension ( $N$ - $\Delta$ ) curves considering fracture process.

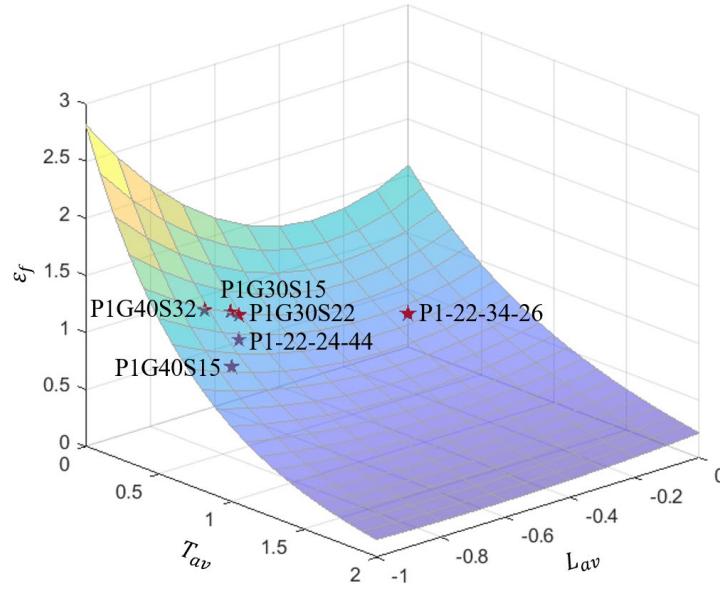
Table 6-7 summarizes the fracture prediction results for all the bolted connections, and these include both the average stress triaxiality  $T_{av}$  and the average Lode parameter  $L_{av}$  at fracture initiation point, and the predicted deformations of fracture initiation  $\Delta_{f, predict}$ . It should be noted that the measured deformations of fracture initiation  $\Delta_f$ , which is defined as the point of sudden change of slope in the measured load-extension curve, are also provided for comparison. Errors of the predicted fracture displacements are calculated by Eq. 6-2 as follows, and their values are also summarized in Table 6-7.

$$Error = \left| \frac{\Delta_{f, predict} - \Delta_f}{\Delta_f} \right| \quad (6-2)$$

Table 6-7 Fracture prediction of bolted connections.

| Bolted connections |             | $\varepsilon_f$ | $T_{av, f}$ | $L_{av, f}$ | $\Delta_{f, predict}$<br>(mm) | $\Delta_f$<br>(mm) | Error<br>(%) |
|--------------------|-------------|-----------------|-------------|-------------|-------------------------------|--------------------|--------------|
| Type A             | P1G40S32    | 0.888           | 0.397       | -0.810      | 3.429                         | 3.270              | 4.9%         |
|                    | P1G30S22    | 1.099           | 0.412       | -0.711      | 3.257                         | 3.314              | 1.7%         |
|                    | P1G30S15    | 1.137           | 0.397       | -0.730      | 3.585                         | 3.421              | 4.8%         |
|                    | P1G40S15    | 0.817           | 0.585       | -0.811      | 3.551                         | 3.514              | 1.0%         |
| Type B             | P1-22-34-26 | 0.781           | 0.560       | -0.254      | 3.105                         | 3.205              | 3.1%         |
|                    | P1-22-24-44 | 0.944           | 0.482       | -0.742      | 3.681                         | 3.653              | 0.8%         |

The states of stresses and strains at fracture, i.e.  $T_{av, f}$ ,  $L_{av, f}$  and  $\varepsilon_f$ , are plotted along with the three-dimensional fracture surface determined by the proposed ductile fracture model in Figure 6.21.



**Figure 6.21** States of stress and strain of fracture initiation of bolted connections.

Taking Type A bolted connection P1G42S32 and Type B bolted connection P1-22-34-26 as examples, the deformed shapes of their models are plotted at various deformation stages during loading in **Figures 6.22** and **6.23**. It should be noted that

- In Model P1G42S32, fracture is initiated near the edges of the bolt holes of the main plate, as shown in **Figure 6.22b**), with a predicted deformation of 3.429 mm. This value is shown to match closely with the measured value of 3.240 mm, i.e. with an error at merely 5.8%. It is also found that the cracks propagate from the edges of the bolt holes to the edges of the main plate, and then, the net section of the main plate between two bolt holes is completely fractured, as shown in **Figure 6.22e**). It should be noted that the path of crack propagation is very similar to the observed one in the test, i.e. from Points P1 to Points P2.
- In Model P1-22-34-26, fracture is initiated in the region between transverse bolt holes, as shown in **Figure 6.23b**), with a predicted deformation of 3.105 mm. This value is

shown to match closely with the measured value of 3.205 mm, i.e. with an error at merely 3.1%. It is also found that the cracks propagate progressively through various net sections between pairs of bolt holes. A complete fracture takes place when two vertical cracks have been fully developed within the main plate, as shown in Figure 6.23e). It should be noted that the path of crack propagation is very similar to the observed one in the test, i.e. from Points P1 to Points P2, and then to Points P3. Therefore, all the data from the models and the tests of these two types of bolted connections are demonstrated to agree closely with one another throughout various stages of fracture, i.e. from fracture initiation, to crack propagation, and then up to fracture failure. Hence, fracture behaviour of these bolted connections between the S690 steel plates is readily predicted by the proposed ductile fracture model.

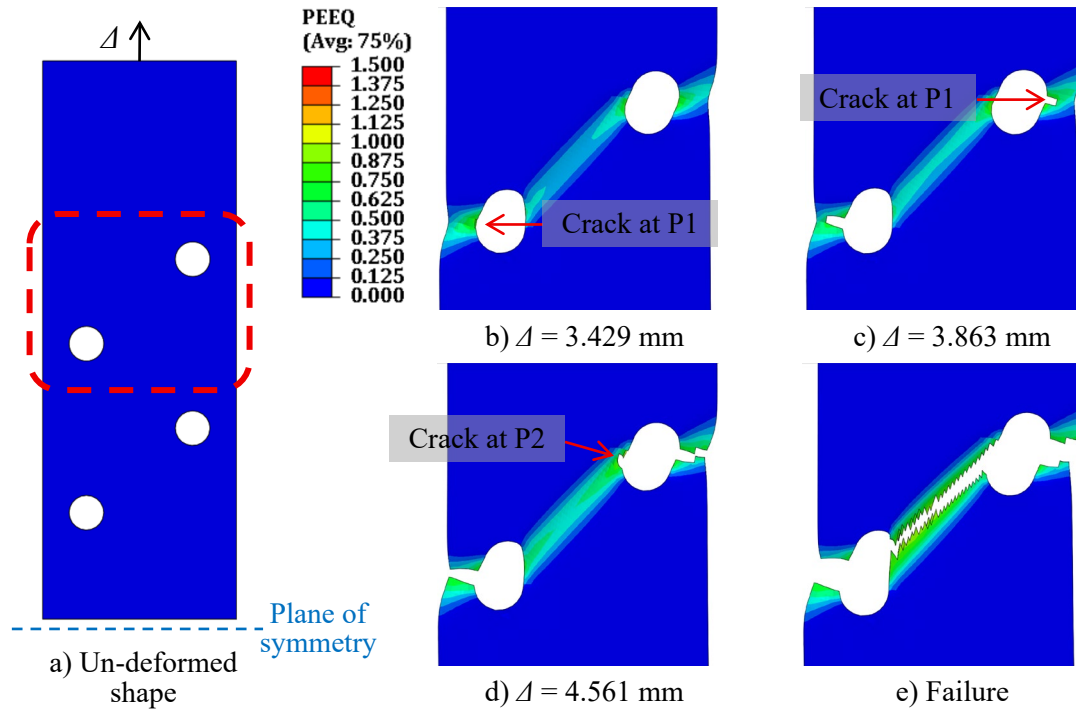


Figure 6.22 Simulated fracture process of specimen P1G40S32.



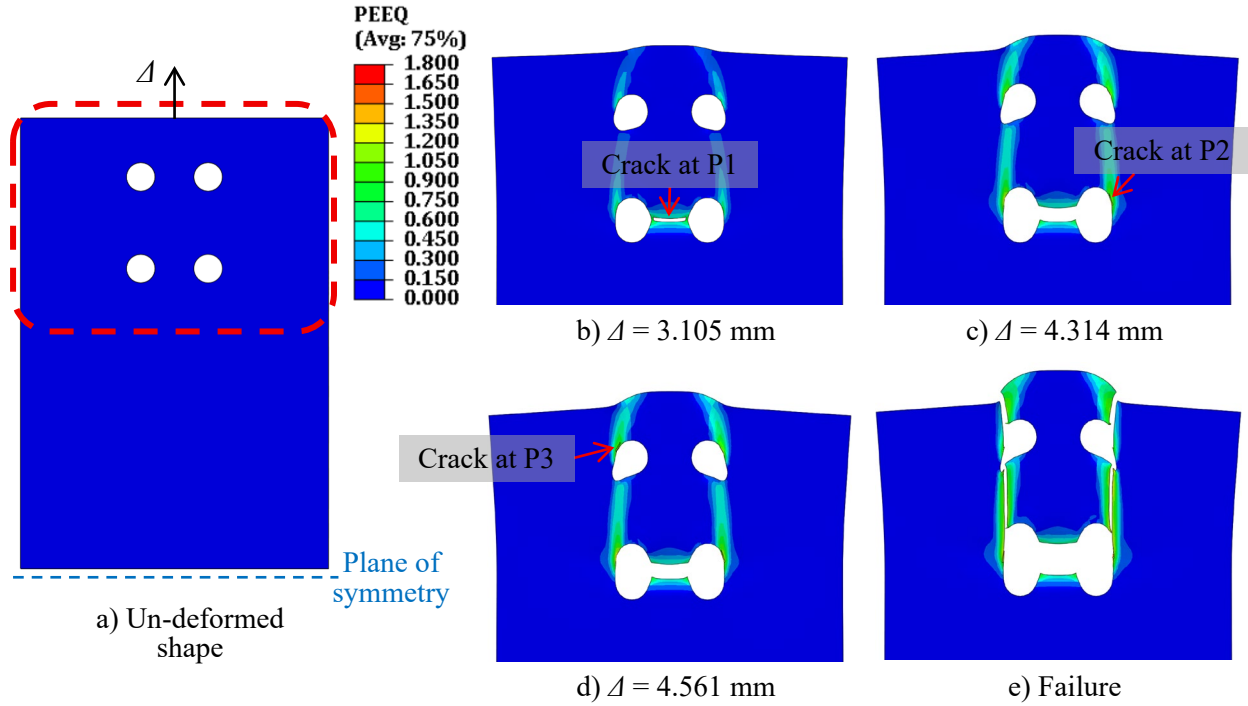


Figure 6.23 Simulated fracture process of specimen P1-22-34-26.

## 6.6 Conclusions

In this chapter, the proposed ductile fracture model in Chapter 5 is calibrated to predict both fracture initiation and crack propagation of the S690 bolted connections. Six different types of notched coupons, i.e. a total of 39 coupons, were designed and tested under tensile loadings to generate different tension and shear stresses. Examination on fractured specimens using scanning electron microscopy (SEM) was also conducted to identify microstructural differences between fracture triggered by i) tension actions, ii) shear actions, and iii) combined tension and shear actions. Advanced numerical models were then established to simulate these coupons under various combinations of tension and shear actions. It should be noted that critical states of stress and strain of these coupons at fracture initiation were readily identified in these models to calibrate the proposed ductile fracture model. The proposed ductile fracture model was then applied to assess the structural

behaviour of two different types of bolted connections of the S690 steel plates. Fracture initiation is readily assessed with a fracture strain  $\varepsilon_f$  which is specifically calculated from the proposed fracture model, i.e. the fracture loci in the  $T_{av} - L_{av} - \varepsilon_f$  space. Good comparison among the results of both the models and the tests is achieved. It should be noted that:

- 1) The proposed ductile fracture model has been carefully calibrated against data of 39 notched coupon tests as well as their numerical simulations under various stress states (tension, shear, as well as combined tension-shear actions). A number of fracture loci in the  $T_{av}-L_{av}-\varepsilon_f$  space have been established according to various calibrated parameters obtained from the coupon tests, and the fracture loci are able to describe effectively the dependence of fracture strain  $\varepsilon_f$  on the stress triaxiality  $T_{av}$  and the Lode parameter  $L_{av}$ .
- 2) A systematic SEM observation on fracture surfaces of the tested coupons under various actions revealed distinct failure modes: i) dimpled surfaces under large  $T_{av}$  and large  $L_{av}$ , ii) transitional features at medium  $T_{av}$ , and iii) smooth surfaces under small  $T_{av}$  and small  $L_{av}$ .
- 3) Based on the fracture loci in the  $T_{av}-L_{av}-\varepsilon_f$  space established with calibrated parameters obtained from the coupon tests, the fracture behaviour of the S690 bolted connections have been predicted successfully by the selecte proposed ductile fracture model. Both the locations of fracture initiation and the corresponding deformations of the steel plates in the vicinity of the bolt holes were predicted with a high level of accuracy. The prediction errors of the deformations of fracture initiation are found to be within 5% for all the examined bolted connections.

- 4) The paths of crack propagation in both the net-section failure mode in Type A bolted connections and the block shear failure mode in Type B bolted connections have been successfully simulated with advanced numerical models which incorporate the proposed fracture model. The simulated paths of crack propagation in both types of bolted connections are demonstrated to agree closely with the experimental observations.

## Chapter 7

### Conclusions and future work

#### 7.1 Introduction

This research aims to advance the understanding on the ductile fracture behaviour of high strength S690 steel under large deformations and complex stress states. The investigation employs an integrated experimental and numerical approach to examine stress and strain conditions at various deformation stages of the S690 steel. A multi-scale approach is adopted in the experimental investigation, and various experimental techniques are adopted in a rational and coherent manner, including digital imaging in tensile tests, *in-situ* X-ray computed tomography (CT) scanning and loading in detection of micro-voids, and digital 3D reconstruction in quantifying void volumes and areas in the necking regions. Advanced numerical models have also been established to evaluate the stress triaxiality and the Lode parameters within the necking regions.

In the material level, Chapter 3 presents a robust and efficient modified Bridgman method to determine accurately true stress-strain curves of structural steel after necking which incorporates the effects of non-uniform distributions of stresses and strains within the steel. Building upon this, Chapter 4 presents an investigation which utilizes *in-situ* X-ray CT technique to examine micro-voids evolution during loading, and their dependence on stress triaxiality and equivalent plastic strain is quantified. The investigation provides essential insights of the presence of micro-voids to the macro fracture behaviour. After recognizing limitations of existing ductile fracture models, a new ductile fracture model is presented in Chapter 5, and the proposed ductile fracture model is developed successfully

for the S690 steel to predict fracture under combined tension and shear actions. Finally, the proposed model is demonstrated successfully in Chapter 6 to predict fracture initiation and propagation in a total of six bolted connections with two different connection configurations. Deformation characteristics together with the paths of fracture propagation of these bolted connections have been predicted. Comparison between the predicted and the observed results is shown to be highly satisfactory. Key findings of this research are presented in the following sections while a plan for future work is also provided.

## 7.2 Key findings

- 1) A Modified Bridgman Method is proposed and validated to determine true stress–strain ( $\sigma_t$ – $\varepsilon_t$ ) curves for structural steels from S355 to S960. By deriving a cubic function between the spatial necking radii ratio  $a/R$  and the average strain  $\varepsilon_{av}$ , the method eliminates laborious measurement of  $R$  and incorporates material parameters, i.e. the necking strain  $\varepsilon_n$ . The predicted load–extension ( $N$ – $\Delta$ ) curves from numerical simulations show that the Modified Bridgman method consistently outperforms the original Bridgman method, which underestimated true stresses, and provides accuracy comparable to the Instantaneous Area Method (*iAreaM*) with significantly reduced computational effort. It is confirmed that post-necking stress distributions at critical cross-sections are governed by average strains at necking, which is often numerically close to the hardening exponent  $n$ , supporting the universal applicability of the proposed  $a/R$  expression to different steel grades.
- 2) The evolution of micro-voids under tension is quantified using in-situ X-ray CT scanning and corresponding numerical modelling. A continual micro-void evolution is

evident after onset of necking, with the majority of voids formed in the vicinity of minimum cross-sections. Coupon S355 exhibits larger numbers and sizes of micro-voids than S690 and S960, with a substantially larger void volume fraction near critical cross section. However, the void volume fractions at initiation of fracture for all tested coupons are very small ( $\leq 0.2\%$ ), making it difficult to relate these directly to fracture in the coupons, especially for Coupons S690 and S960. Moreover, micro-void growth is demonstrated to be highly dependent on stress triaxiality  $T$  and equivalent plastic strain  $\varepsilon_{eq}$ , and the Rice–Tracey formula is able to effectively describe micro-void evolution throughout loading process. Comparisons between measured void-related parameters, i.e.  $f_0$ ,  $f_N$ ,  $f_c$  and  $f_f$ , and those calibrated in the literature for the GTN model reveal significant differences due to assumptions and simplifications in calibration.

- 3) A new ductile fracture model is developed to capture the effects of combined tension and shear actions. Tests on 34 carefully designed notched specimens, together with advanced three-dimensional solid-element simulations, identify critical stress and strain states at fracture over a wide range of stress triaxiality and Lode parameters. SEM observations show that ductile fracture triggered by shear stresses has a unique microstructure, i.e. smooth fracture surface no dimples, whereas combined tension–shear leads to both dimples and layered features. Fracture strain  $\varepsilon_f$  increases with a decrease in stress triaxiality  $T$ , with a more pronounced increase at pure tension than at plane strain. With the increasing Lode parameter  $L$ ,  $\varepsilon_f$  decreases first and then increases. Five modern ductile fracture models calibrated with the test data produce fracture surfaces with significant errors and fail to describe the behaviour satisfactorily. Accounting for the non-linear relationship between  $\varepsilon_f$  and the Lode parameter, the

proposed model accurately describes the ductile fracture of S690 steel under combined tension and shear actions.

- 4) The proposed ductile fracture model, calibrated against 39 notched coupon tests under tension, shear, and combined tension–shear actions, is applied to predict fracture initiation and crack propagation in S690 bolted connections. Based on the fracture loci in the  $T_{av} - L_{av} - \epsilon_f$  space established with those calibrated parameters obtained from the coupon tests, the fracture behaviour of the S690 bolted connections have been predicted successfully by the proposed ductile fracture model. Both the locations of fracture initiation and the corresponding deformations of the steel plates in the vicinity of the bolt holes are predicted with a high level of accuracy. The prediction errors of the deformations of fracture initiation are found to be within 5% for all the examined bolted connections. Moreover, the paths of crack propagation in both the net-section failure mode in Type A bolted connections and the block shear failure mode in Type B bolted connections have been successfully simulated with advanced numerical models which incorporate the proposed fracture criterion. The simulated paths of crack propagation in both types of bolted connections are demonstrated to agree closely with the experimental observations.

Overall, the project delivers:

- A robust and efficient workflow to derive true stress–strain curves for structural steels after the onset of necking up to initiation of fracture.
- A relationship between micro-void nucleation, growth, and coalescence and the evolving stress–strain state, validated by in-situ CT scanning and finite element simulations.

- A fracture model that captures the coupled roles of stress triaxiality and Lode parameter under combined tension and shear actions, overcoming limitations of existing formulations.
- Demonstrated predictive capability for fracture initiation and crack propagation in high-strength steel bolted connections, supporting reliable and optimized use of S690 steel in structural applications.

Hence, this research enables a safe design for high strength S690 steel members and connections, mitigates premature fracture, and supports a broad adoption of the high strength steels in cost-effective, lightweight, and resilient structural systems.



### 7.3 Future works

Based on the findings of this research, future work will focus on extending the investigation to welded connections of the S690 high strength steel with emphasis on the ductile fracture behaviour of the heat-affected zones (HAZ). The planned research includes:

- 1) Experimental investigation of ductile fracture behaviour of HAZ:
  - To conduct *in-situ* X-ray computed tomography (CT) scanning and loading on the S690 coupons after heat treatment in such a way that their microstructure are simulated physically to those critical sub-zones within various heat affected zones in the welded sections.
  - To conduct tensile tests on notched coupons after heat treatment under combined tension and shear actions.
- 2) Advanced numerical modelling:
  - To establish advanced finite element models to simulate structural behaviour of both coupons of the welded sections and welded T-joints.
  - To propose a ductile fracture model after calibrating specifically to the HAZ using test data of coupons of welded sections.
- 3) Prediction of fracture in welded joints:
  - To apply the calibrated model to predict fracture initiation and propagation in the S690 welded T-joints under complex stress states.
  - To establish accuracy of the predicted results against the test results.

This work will deliver a comprehensive ductile fracture model which is able to predict fracture behaviour in welded connections of the S690 high strength steel, enhancing safety predictions of the S690 steel structures.

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