Compressive behaviour of engineered cementitious composites (ECC) under high strain rates

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**Compressive Behaviour of Engineered
Cementitious Composites (ECC) under High
Strain Rates**

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SCHOOL OF CIVIL AND ENVIRONMENTAL ENGINEERING

COLLEGE OF ENGINEERING

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SCHOOL OF CIVIL AND ENVIRONMENTAL ENGINEERING

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SUMMARY

In the past few decades, an interest in exploring a new class of cementitious composite called engineered cementitious composites (ECC) has been gaining ground. A considerable number of studies have indeed shown that ECC offers superior attributes compared to normal concrete in many aspects, including tensile strength and strain capacity, toughness, and durability. These findings surely suggested that there is a great potential for its application as a modern construction material, especially for structures requiring considerable amount of toughness and ductility, i.e. structures subjected to extreme loading conditions such as earthquake, impact and blast.

In the present study, ECC dynamic response under compressive loading is of particular interest since the initial stress imposed on the target is in the form of compressive stress wave when impact or blast loads attack a target structure. Hence, if the material has insufficient compression capacity, the target structure may be severely crushed at the initial stage—even before the incident waves are reflected as tensile stress waves. In addition, the compressive behaviour of concrete-like materials can actually be associated to the tensile behaviour since the compression failure is governed by the formation of tensile cracking based on Griffith theory of brittle fracture.

The Split Hopkinson Pressure Bar (SHPB) or Kolsky Bar is an apparatus commonly used for determining mechanical properties of materials at high strain rate. Cylindrical samples have been traditionally and routinely used as SHPB test samples due to their ease of machining on a lathe for solid metal samples. For cementitious composites, however, cuboidal specimens may be preferable than cylindrical samples due to these reasons: (1) smooth, flat, and parallel contact surfaces are formed immediately once the specimen is hardened, thus surface grinding process is no longer necessary and the specimen preparation process can be expedited; (2) the flat face of cuboidal specimens facilitates better surface mapping for high-speed imaging, hence allows the use of optical measurement technique. Since limited information is available to assess the suitability of using cuboidal specimens for SHPB tests, the comparison of compressive behaviour between cylindrical and cuboidal specimens is presented and the factors that may cause discrepancies on the results are explained in this study.

The outcomes of the research indicate that the cuboid and the cylinder specimens will show comparable dynamic stress-strain curves in the early stage of loading (i.e. low strain regime), but will disclose noticeable difference at the later stage of loading (i.e. large strain regime, after the test specimens reaching the peak capacity). The phenomenon is attributed to the change in boundary friction condition at the bar-specimen interfaces. At the beginning of the test, the boundary friction can be kept to a minimum owing to sufficient amount of lubricant applied at the interfaces. Nonetheless, at the latter stage when the specimen has undergone significant deformation, the boundary friction will gradually increase due to diminishing thickness of the lubricant layer—the lubricant might be squeezed out or might be absorbed into flaws or cracks formed in the specimen at large deformation. Further examination of the failure crack patterns with the aid of a high-speed camera suggests that the cylinder specimens offer more reasonable and reliable results than the cuboid specimens; while the cylinder specimens are characterized by a splitting tensile failure mode, the cuboidal specimens are characterized by a combination of splitting tensile with corner or diagonal failure mode.

The comparison of dynamic compressive behaviour between ECC and mortar is also investigated in this study. It is evident that ECC still outperforms mortar even at high strain-rate conditions. The post-peak branches of ECC stress-strain curves display gentler slope than that of mortar, indicating ECC greater energy absorption capacity. The recovered specimens at the end of the tests also indicate less catastrophic damage appearing in ECC specimens; ECC specimens break up into few large fragments, but mortar specimens shattered into many small fragments. Finally, the rate-sensitivity of ECC mechanical properties—in terms of the compressive strength and the critical strain—is discussed and the empirical DIF formula useful as inputs for impact or blast simulation is derived.

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LIST OF SYMBOLS

One-dimensional Elastic Stress Wave Theory

ε = strain

σ = stress

C_0 = elastic wave propagation velocity

f = frequency

u = displacement

x = position

t = time

Propagation of elastic wave in SHPB tests

ε_i = incident strain wave

ε_r = reflected strain wave

ε_t = transmitted strain wave

ε_s = strain in the specimen

$\dot{\varepsilon}_s$ = strain rate in the specimen

σ_s = stress in the specimen

A = cross sectional area of the pressure bars

A_s = cross sectional area of the specimen

C_0 = elastic wave propagation velocity in the pressure bars

E = elastic modulus of the pressure bars

l_s = length of the specimen

d_s = diameter of the specimen

CHAPTER 1: INTRODUCTION

1.1. Background

During its service life, a structure may be exposed to various kinds of environment and loading conditions (Banthia, 1987). Loadings can be classified broadly into two categories: quasi-static loading and dynamic loading. The behaviour of structures under static loading case had been extensively investigated by many researchers in the past centuries. Also, the analysis procedure to predict structural responses under static load is relatively more straightforward compared to that under dynamic excitation.

The need to investigate the behaviour of structures under dynamic loading arises due to several reasons as follows:

- natural disaster (earthquake, hurricane, rock fall)
- manmade disaster (terrorist attack and war)
- accidental load (gas explosion)
- design innovation (trend towards slender and lightweight structures which are relatively more susceptible to dynamic excitation by wind or moving load)
- exposure to other external sources of vibrations (trains, quarries, construction activities, etc.)

The first three cases (natural disaster, manmade disaster, and accidental load) most often cause disastrous damages and lead to mass casualty incidents due to sudden nature of their occurrences. It is, therefore, not surprising that most research in the recent decades focuses on how to minimize damage and increase the survivability of structures under such cases. Of particular interest is the investigation on structural response under an explosion. This is because an explosion is characterized by a much higher loading rate compared to that of earthquake, wind, and moving load (Feldman, et al., 1962). In addition, it is often accompanied by impacting fragments, ground shock, fire, nuclear radiation and any other devastating effects which may further aggravate the initial damages. Furthermore, in most cases, an explosion may trigger partial or total collapse of a structure. The collapse of Ronan Point apartment building, Alfred P. Murrah federal

building, and World Trade Centre twin towers are the most notable examples of progressive collapse initiated by an explosion.

The foregoing discussion is especially relevant for construction of national critical infrastructures, such as government buildings, embassies, and military facilities, which (from structural point of view) should be able to prevent catastrophic failure and protect its content (personnel and equipment) from the effect of blast, ground shock, debris, fragment, thermal radiation, nuclear radiation, and electromagnetic pulse (Krauthammer, 2008). According to UFC3-340-02 (Department of Defence, 2008), the effect of blast overpressure frequently governs the behaviour of structures although, at some cases, primary or secondary fragments, ground shock, and any other effects may assume equal importance in the design and planning of protective structures. Essentially, structural elements need to have large energy absorption capacity and are sufficiently massive in order to withstand blast load. For this reason, reinforced concrete is a more preferred constructional material over structural steel for conventional construction of protective structures (Smith & Hetherington, 1994).

Several design guidelines and manuals for the design of protective structures are already available, such as tri-service manual TM5-1300 (Department of the Army, 1990), which has been superseded by UFC3-340-02 (Department of Defence, 2008), and ASCE manual 42 (ASCE, 1985). It is noted that UFC3-340-02 is the most widely used publication for the design of both military and civilian structures because it has been approved for public release with unlimited distribution. The manual has provided engineers with step-by-step guidelines and has equipped them with considerable information regarding the design of blast-resistant structures. Nonetheless, as has been pointed out by several other authors (Krauthammer et al., 1994; Woodson, 1994), the design procedure suggested by the manual still has room for improvement especially for the design of joints and plastic hinge regions. In addition, the manual has not addressed the potential use of modern construction materials such as high performance fibre reinforced cementitious composites (HPFRCC) which has relatively high energy-absorption capacity, hence is suitable for state-of-the-art design of protective structures.

A high production cost of HPFRCC (mostly associated to the manufacturing cost of the fibre)—among other factors such as lack of clear guidelines for the use of high-performance concrete and uncertain long-term performance—hinders its widespread application. To overcome this issue, Li et al. (2001) using principle of micromechanics

developed a new class of HPFRCC with extreme tensile ductility of several percent by utilizing relatively inexpensive fibre (i.e. PVA fibre) with minimum fibre volume content (i.e. less than 2%). This HPFRCC is now well-known as engineered cementitious composites (ECC). Due to its outstanding tensile ductility performances, ECC is often considered as one of the most promising modern construction materials suitable to withstand extreme loading conditions, such as under earthquake, impact, and blast. Unfortunately, excellent abilities of ECC have been investigated mostly under quasi-static conditions. Very limited information is known for ECC performances under dynamic loading, especially in the range of impact and blast loads, i.e. strain rate of 1-1000/s. There is a possibility that ECC superior performance will be compromised under dynamic events, thus further investigations have to be conducted.

The initial stress wave imposed on target structure by impact or blast load is primarily compression although it is reflected as tensile wave upon reaching the free boundary. The shear wave component that may cause punching shear failure is also generated during this dynamic event. Thus, dynamic material tests under various loading types—compression, tension, bending, shear, and even torsion—are necessary to gain a comprehensive understanding of ECC dynamic responses.

However, with the current state-of-the-art of standard material testing facility, it is quite challenging to perform dynamic experimental studies. A typical hydraulic, servo-hydraulic, or pneumatic machine can only achieve a maximum strain rate of 0.1 s⁻¹. Beyond this value, dynamic loads must be generated by other means, such as impact, detonation of explosives, pulsed-laser, or other sources of radiation (Meyer, 1994). The most commonly used testing equipment to study material behaviour under high strain rate deformation (i.e. 10² - 10⁴ s⁻¹) is the Split Hopkinson Pressure Bar (SHPB). It is originally developed for compression test, but later is modified for tension, shear, and torsion tests as well. The main challenges for conducting non-compressive dynamic test include developing a special clamping device to hold the specimen in place without perturbing the signal of the loading wave or causing error in the strain measurement; the plastic deformation occurring in the grip section may lead to erroneous stress and strain measurements. In addition, a special geometry of the specimen to accommodate gripping and to ensure homogeneous deformation (e.g. dog-bone or coupon specimens with threaded-end) complicates the specimen fabrication process. Therefore, as a

starting point to explore the use of ECC for protective structures, the present study focuses mainly on dynamic material tests under compression.

The specimen geometry used for SHPB compressive test is traditionally cylindrical because of ease of machining on a lathe for fabrication of cylindrical solid metal specimens. Nonetheless, for cementitious specimen, cube or rectangular-sided prism (i.e. the cuboidal shape) may be more favourable than the cylindrical shape because smooth, flat contact surfaces will be formed automatically once the specimen is hardened. Thus, using cuboidal specimen eliminates the necessity for grinding the specimen. Moreover, with the increasing use of optical measurement technique, the flat face of square-sided samples facilitates better surface mapping, hence allowing more accurate field-strain measurements. Nonetheless, limited information is available to assess the suitability of using cuboidal specimens for SHPB tests. Thus, there is also a need to investigate this topic.

1.2. Objectives

Primary Objectives:

- To obtain compressive stress-strain relationships of ECC at high-strain rates in the range of impact and blast loading
- To investigate the effect of strain rate on the behaviour of ECC
- To provide input material data in terms of DIF useful for numerical simulations of ECC subjected to impact or blast loads

Secondary Objectives:

- To investigate specimen shape effect on SHPB compressive test results

1.3. Scopes

The experimental programme is primarily aimed to investigate the compressive behaviour of ECC when subjected to hard impact and blast loading (i.e. strain rate in the range of 100-1000/s). However, due to limited elastic capacity of the pressure bar, the resulting strain rate attained in this study is limited to 200-600/s.

The constituent material and the mix design of ECC used in this study are chosen such that ECC can be produced using resources easily obtained in Singapore. Silica sand and

fly ash, the two constitutive materials commonly used in typical ECC mix design, is replaced by river sand and ground-granulated blast furnace slag (GGBS), respectively.

1.4. Methodologies

The methodology of the research primarily consists of two main activities: literature review and experimental study. The literature review is conducted using textbooks, journals papers, codes, and national standards to gain detailed knowledge on the development and performance of ECC material and to comprehend how the split Hopkinson pressure bar technique works. Subsequently, the experimental study is performed to extract ECC dynamic responses and to discern the effect of geometrical shape of the SHPB specimen. To achieve these goals, both quantitative data (i.e. stress-strain profiles) and qualitative data (i.e. digital images) with the aid of high speed camera are examined.

1.5. Organization of thesis

This thesis is divided into 6 Chapters as follow.

- Chapter 1: Introduction

This chapter presents the background, objectives, scope of study, and methodology of the research.

- Chapter 2: Review on engineered cementitious composites (ECC)

This chapter contains a review of past published works related to ECC including the historical development, the principle of micromechanics, various parameters affecting ECC performances, and its mechanical responses under quasi-static and dynamic conditions.

- Chapter 3: Review on split Hopkinson pressure bar (SHPB) technique

This chapter elaborates past published works related to SHPB compression test including its historical development, theoretical background, data processing technique, dispersion correction technique, and other necessary conditions for valid SHPB tests.

- Chapter 4: Experimental programme

In this chapter, detailed descriptions of the specimen fabrication technique (i.e. mix design, specimen dimensions, specimen surface finishing, etc.), the SHPB test setup (i.e. pressure bar dimensions, design of the gas gun system and instrumentation), and the SHPB test procedure adopted are presented.

- Chapter 5: Results and discussion

In this chapter, the experimental results are presented and the significance of the results is emphasized.

- Chapter 6: Conclusion

Finally, in the last chapter, the conclusions and recommendations for future work are presented.

CHAPTER 2: LITERATURE REVIEW ON ENGINEERED CEMENTITIOUS COMPOSITES (ECC)

2.1. Introduction

This chapter summarizes the development and past research studies on engineered cementitious composites (ECC) as a modern construction material. The unique characteristics of ECC and its potential structural applications, especially in relation to its resistance against impact and blast loading, are emphasized.

2.2. The development of ECC

As the most widely used construction materials in the world, concrete clearly shows that it has competitive advantages over the other major construction materials, such as steel, timber, asphalt, and bricks. There are several reasons for its dominance, viz. low-cost, low-maintenance, adaptability (i.e. ability to be cast to any shape), and widespread availability. For the design and construction of protective structures, in particular, concrete also possesses superior attributes, e.g. massive, durable, relatively inert, and fire-resistant. Nonetheless, the brittle nature of concrete may pose some serious problems when structural concrete components are subjected to extreme mechanical and/or environmental loading conditions.

To overcome brittleness in concrete, short/chopped fibres—which act as “crack arresters”—are often added into the mix. The addition of fibres as reinforcement in building material is not a revolutionary technique. Since ancient times, straws and horse hair had been used in the fabrication of mud-brick and lime mortar to minimize cracking and to improve the integrity of structures. Nonetheless, the addition of fibre in concrete material only became increasingly popular in the 1950s, when Romualdi and co-workers (Romualdi & Batson, 1963; Romualdi & Mandel, 1964) demonstrated that the presence of closely spaced random fibre in concrete can effectively reduce the brittleness of concrete. Since then, the knowledge and the use of concrete with fibre incorporation have been continuously developed to the establishment of modern fibre-reinforced concrete (FRC) that we know today.

Although the mechanical properties of concrete—particularly, in relation to the energy absorption capacity (i.e. the toughness) and ability to control crack—have been tremendously improved by the addition of fibre, tensile ductility (i.e. strain capacity) of FRC remains relatively low due to its tension-softening behaviour (Li, 2008). Fig. 2.1 shows that under uniaxial tension, the load capacity of FRC decreases immediately after a crack forms and grows unstably. Extensive research to further improve the performance and to better understand the behaviour of FRC has been conducted and resulted in the development of a new class of cementitious composites called high-performance fibre-reinforced cementitious composites (HPFRCC). The performance of HPFRCC is different from that of FRC in that HPFRCC exhibits tensile strain hardening behaviour similar to that of steel (Fig. 2.1). The distinct behaviour of HPFRCC has a significant influence on the overall performance of structures made of this type of materials.

Fig. 2.1 Comparison of stress-deformation curves between concrete, FRC, and HPFRCC under uni-axial tension (Li, 2008)

The HPFRCC itself has been segmented into many different sub-classes depending on their development approach. The two most well-known sub-classes are Ductal® and engineered cementitious composites (ECC). Ductal® was developed by a formation of a tightly-packed dense matrix that would improve both tensile and compressive properties of the material. The addition of fibres in Ductal®'s matrix was intended to annihilate its brittleness. On the other hand, ECC was developed based on the principle of micromechanics that promoted synergistic interactions among all the constituent

materials—matrix, fibres and their interfaces. The micro-mechanics theory, which establishes the relation between the macroscopic mechanical properties of heterogeneous composites to its constituent's microstructures, is the backbone of the material design approach of ECC. With the aid of micro-mechanics theory, the mechanical properties of ECC can be customised and the use of fibres can be optimised. Since ECC utilizes less amount of fibres (typically less than 2% by volume), its usage is more economical compared to Ductal®. In the following sections, the basic theory of micromechanics as the basis of the development of ductile ECC and the potential applications of ECC are discussed.

2.3. Micromechanical criteria for ductile fibre-reinforced cementitious composites

The fundamental requirement for the development of strain hardening behaviour in heterogeneous composites is the existence of steady-crack propagation—which is a condition whereby a crack increases in length at a constant ambient stress while preventing further opening of its width (i.e. formation of a flat crack as shown in Fig. 2.2). This condition allows the crack to maintain its load-carrying capacity even after its opening. Also, under increasing ambient load, it enables the crack to transfer the load back into the neighbouring matrix, prompting formation of new cracks at different sites. Since numerous cracks are generated during the process, the phenomenon is often called “multiple cracking”.

Fig. 2.2 Griffith type crack vs. steady state flat crack (Li, 2008)

Marshall & Cox (1988) using J-integral method showed that the steady state flat crack propagation in fibre-reinforced composites was governed by energy balance between the external work done (by the applied load) and the internal energy absorbed (by the fibre and the matrix during the opening at the apex of the crack). Their works established a general relationship for steady-state cracking in reinforced composites, which can be expressed mathematically by Eq. 2.1 and graphically by Fig. 2.3. Li (2003) labelled this as “energy criterion”, which governs the crack propagation mode of fibre-reinforced composites.

$$J_{tip} \leq \sigma_0 \delta_0 - \int_0^{\delta_0} \sigma(\delta) d\delta \equiv J_b' \quad (\text{Eq. 2.1})$$

In the above equations,

J_{tip} : the crack tip toughness (shaded area in Fig. 2.3). For low fibre volume fraction, the value can be approximated as equal to K_m^2/E_m (Li et al., 1995), where K_m and E_m are matrix fracture toughness and Young's modulus, respectively

J_b' : the complementary energy (hatched area in Fig. 2.3)

σ_0 : the maximum fibre-bridging stress or the critical stress

δ_0 : the opening of the crack at maximum bridging stress

$\sigma(\delta)$: the relationship between the bridging stress transferred across the crack (σ) and the opening of the crack (δ), i.e. fibre-bridging constitutive law

Fig. 2.3 Typical stress-crack opening relationship (i.e. constitutive law) of fibre-reinforced composites for steady-state cracking (Yang et al., 2008)

Another important consideration for the development of strain-hardening behaviour (in addition to steady state crack propagation mode) is the condition for crack initiation. The matrix cracking—especially the first cracking—must occur at a stress level below the maximum fibre-bridging stress, so that the opening of a crack can be suppressed by the fibre bridging action. In other words, the matrix cracking strength (σ_c), i.e. the tensile strength associated with the cracking of the matrix, must not exceed the maximum fibre-bridging stress (σ_0), as expressed by Eq. 2.2. Li (2003) defined this as the “strength criterion”, which controls the initiation of cracking.

$$\sigma_c < \sigma_0 \quad (\text{Eq. 2.2})$$

Based on both criteria, it is clear that the fibre-bridging constitutive law $\sigma(\delta)$ (**Fig. 2.3**)—that controls the values of complementary energy (J_b') and maximum bridging stress (σ_0) of fibre-reinforced composites—plays a critical role in governing the behaviour of the composites. Compliance to the criteria will lead to tensile strain-hardening behaviour, while violation of the criteria will lead to tensile strain-softening behaviour (as in typical FRC). The criteria also imply that the fibre-bridging constitutive law has a direct, strong correlation to the material stress-strain curves of the composites. Therefore, understanding of variables governing the profile of fibre-bridging constitutive law is important.

2.4. Fibre-bridging Constitutive Law

Extensive research conducted by previous authors (Li & Leung, 1992; Li, 1992; Lin & Li, 1997; Lin et al., 1999) had shown that the profile of the fibre-bridging constitutive law of fibre-reinforced cementitious composite is governed by various variables, which can be broadly classified into three sets of parameters, viz. fibre properties, matrix properties, and interfacial properties. The complete list of the micromechanical parameters is shown in **Table 2.1**. The influences of each parameter individually and collectively (as a result of their interactions) in governing the overall composite behaviour were investigated on the basis of micromechanics theory.

Table 2.1 Micromechanical parameters governing the bridging behaviour of fibre-reinforced cementitious composites

Constituents	Parameters	Remarks
Fibres	V_f = Volume fraction of fibre E_f = Young's modulus of fibre L_f = Length of fibre d_f = Diameter of fibre σ_{fu} = Tensile strength of fibre	
Matrix	V_m = Volume fraction of matrix E_m = Young's modulus of matrix K_m = Fracture toughness of matrix a_0 = Initial flaw size distribution	K_m : experimentally measured from wedge splitting test or three-point bending test (ASTM E399)
Interfaces	G_d = Chemical bond strength τ_0 = Frictional bond strength β = Slip-hardening coefficient f = the snubbing coefficient (to take into account the effect of fibre alignment during pull-out) f' = the strength reduction factor (to take into account the decrease of fibre strength due to arbitrary orientation of the fibre at an angle inclined to the crack plane)	Obtained from a single pullout test
Other	η = the ratio of effective fibre stiffness to effective matrix stiffness (i.e. $\eta = \frac{V_f E_f}{V_m E_m}$)	

The construction of fibre bridging constitutive law, owing to diverse variables and complex mechanisms associated with it, is a complicated process. It requires a deep understanding of the physical and chemical interactions involved between each constituent and demands rigorous mathematical derivations to express these relationships explicitly. The construction of the fibre bridging constitutive law starts with developing a theoretical model for a single fibre pull-out behaviour. It is then followed by averaging the contribution of each fibre with different embedment length and orientations along the crack plane using probability density function. A brief and concise explanation on the development of the fibre-bridging constitutive law is presented in this section. The detailed derivation of the fibre-bridging constitutive law can be found in the works by Lin et al. (1999) and Yang et al. (2008).

The process of a single fibre pullout mechanism can be divided mainly into two stages: de-bonding stage and pullout stage. During the de-bonding stage, the crack propagates along the fibre-matrix interface, creating a tunnel surrounding the fibre. At this stage, both frictional and chemical bond stresses must be overcome for the crack to propagate further. The fibre force-displacement relation at this stage is expressed mathematically by **Eq. 2.3** (Lin et al., 1999; Yang et al., 2008).

$$P(\delta)_{debonding} = \sqrt{(\tau_0\delta + G_d) \frac{\pi^2 E_f d_f^3 (1+\eta)}{2}} \quad (\text{Eq. 2.3})$$

After the fibre is fully de-bonded, it enters the second stage (the pullout mechanism). At this stage, the chemical bond no longer exists; hence, the interface is fully governed by the frictional stress alone. The transition from the de-bonding stage to the pullout stage is characterized by a sudden drop in the force-displacement profile (Lin et al., 1999). The force-displacement relationship at pullout stage is presented in **Eq. 2.4**.

$$P(\delta)_{pullout} = \pi d_f \tau_0 (L_e - \delta - \delta_0) \left(1 + \frac{\beta(\delta - \delta_0)}{d_f} \right) \quad (\text{Eq. 2.4})$$

$$\delta_0 = \frac{2\tau_0 L_e^2 (1+\eta)}{E_f d_f} + \frac{L_e}{E_f} \sqrt{\frac{8G_d E_f (1+\eta)}{d_f}} \quad (\text{Eq. 2.5})$$

Where δ_0 the displacement at which the fibre is fully de-bonded (Eq. 2.5) and L_e is the embedment length. **Eq. 2.3** to **Eq. 2.5** lay the basis of a classic constitutive bridging law of the composite. Other factors that may affect the magnitude of the pullout force, such as the orientation angle of the fibre relative to the crack plane is taken into consideration

via an empirical derivation (**Eq. 2.6**). Morton & Groves (1976) and Li (1992) investigated that the bridging force would increase when the fibre was pulled at an inclined angle; the phenomenon was analogous to the Euler friction pulley system whereby the tension force at the other end of the rope increased with an increase of the contact angle. In addition, Banthia et al. (1997) also found that the fibre orientation might affect the in-situ strength of certain type of fibre. He observed that the fibre rupture strength (σ_{fu}) of PVA fibre decreased when it was pulled at an inclined angle. The effect can be described by a similar empirical form to **Eq. 2.6**, but with negative exponential factor as shown in **Eq. 2.7**.

$$P(\phi) = P(0)e^{f\phi} \quad (\text{Eq. 2.6})$$

$$\sigma_{fu}(\phi) = \sigma_{fu}(0)e^{-f'\phi} \quad (\text{Eq. 2.7})$$

In **Eq. 2.6** and **Eq. 2.7**, ϕ is an arbitrary angle relative to the crack plane, f and f' is referred to as the snubbing coefficient and the strength reduction factor, respectively.

On a composite level, the bridging stress across the crack was supported by a collective action of many fibres along the crack plane. The variations in orientation angle and embedment length of the fibres along the crack plane could be represented by probability density functions as discussed by Wang et al. (1990). The uniform random distribution assumption was deemed valid for short fibre with low volume fraction (Yang et al., 2008). Finally, the composite fibre-bridging constitutive law can be obtained through averaging the contributions of all fibres along the crack plane as defined by **Eq. 2.8**.

$$\sigma(\delta) = \frac{1}{\left(\frac{1}{4}\pi d_f^2\right)V_f} \int_{\phi=0}^{\frac{\pi}{2}} \int_{z=0}^{\frac{L_f}{2}\cos\phi} P(\delta)p(\phi)p(z)dzd\phi \quad (\text{Eq. 2.8})$$

Where $p(\phi)$ and $p(z)$ are the probability density functions of the orientation angle ϕ and the centroidal distance z of the fibres along the crack plane, respectively. The centroidal distance—defined as the distance from the fibre centroid to the crack plane—is included in the model to distinguish the fibres contributing to the bridging actions. It is assumed that fibres with centroidal distance less than $L_f/2$ on either side of the crack will bridge the crack, while those with centroidal distance more than $L_f/2$ will not bridge the crack.

Hence, it is clear that the construction of the fibre-bridging constitutive law of a composite involves knowledge on the mechanics of a single aligned fibre pullout, the mechanics of a single inclined fibre pullout, and the random distribution of fibres (in terms of location and orientation) across the crack plane.

2.5. Application of micromechanical models as ECC material design tool

After the criteria and the fibre-bridging constitutive model for ductile fibre-reinforced cementitious are known, the material design tool based on micromechanical models can be established. The most notable works for the development of ductile fibre-reinforced cementitious composites using micromechanics principle to date was perhaps that conducted by Li (1993) at University of Michigan. Li (1993) used the term “engineered cementitious composites” (or ECC in short) to emphasize the use of engineering design principle (i.e. micromechanics theory) behind the development of this material. With moderate fibre volume fraction, ECC developed by Li and his research team (Li et al., 2001; Li et al., 2002) not only exhibits superior tensile performance to normal FRC, but also offers more cost-effective and easy-to-process cementitious composites. In this section, several design scenarios to demonstrate the use of micromechanical models as an analytical tool for ECC material design is outlined.

It is clear from **Eq. 2.1** and **Eq. 2.2** that a sufficient margin between the complementary energy (J_b') and the crack tip toughness (J_{tip}), as well as between the maximum fibre-bridging stress (σ_0) and the matrix cracking strength (σ_c), is the key factor to achieving multiple cracking behaviour in a fibre reinforced composite. There are primarily two ways to attain this condition: (1) increasing J_b' and σ_0 by tailoring the interfacial parameters governing the fibre-bridging constitutive laws (**Eq. 2.8**) and/or (2) decreasing J_{tip} and σ_c by modifying the matrix fracture toughness or the flaw size distribution of the matrix. The former way is more desirable than the latter since reducing the matrix toughness and introducing artificial flaws in the matrix as a mean to change its flaw size distribution imply low first crack strength which in turn may violate serviceability limit state criterion.

(1) Tailoring interfacial bond properties (τ_0 and G_d)

Tailoring frictional bond strength (τ_0)

Let us now consider the simplest case of interface tailoring when the fibre-matrix interaction of the composite is governed by only frictional bond (the condition typically

happens in cementitious composites reinforced by hydrophobic fibres, such as polypropylene/PP fibres, i.e. $\tau_0 > 0$ and $G_d = 0$).

The effect of frictional bond strength (τ_0) on the composite behaviour can be studied from the work by Yang & Li (2010). In their work, the influence of τ_0 was presented in terms of the minimum amount of fibre required to satisfy the strength and the energy criteria (i.e. $V_{f,crit}$). In this manner, not only the influence of τ_0 in changing the behaviour of composite could be deduced, but the role of micromechanics as a tool for tailoring a cost-effective, high-performance composite could also be demonstrated.

Fig. 2.4 The influence of frictional stress on the critical volume fibre fraction of a cementitious composite reinforced by PP fibres (a) based on strength and energy criterion respectively (b) based on both criteria combined (Yang & Li, 2010)

For a given set of micromechanical parameters (e.g. $E_f, L_f, d_f, \sigma_{fu}, \beta, f, f'$), the $V_{f,crit} - \tau_0$ relationship can be derived by adopting the micromechanical model as described in **Section 2.3** and **Section 2.4**. Yang & Li (2010) through graphical representation described the $V_{f,crit} - \tau_0$ relationship as shown in **Fig. 2.4**. It was noted that although the $V_{f,crit} - \tau_0$ relationship derived did not universally apply to all kinds of fibre reinforced cementitious products—as it was dependent on various parameters specified—it was sufficient to illustrate the effect of frictional force in governing typical fibre/cement composites.

The solid line in **Fig. 2.4** shows the $V_{f,crit} - \tau_0$ relationship when the strength criterion is fulfilled. It is shown that at low friction zone ($\tau_0 < 0.3$ MPa), the decrease of τ_0 leads to

an increase of $V_{f,crit}$, while at high friction zone ($\tau_0 \geq 0.3 \text{ MPa}$), the change of τ_0 has no effect on the value of $V_{f,crit}$. Yang & Li (2010) explained that the distinct sensitivity of $V_{f,crit}$ to τ_0 at different friction zones was caused by dissimilar mechanisms governing the bridging behaviour. When the interfacial friction was low, the fibre was able to slide through the matrix more easily, allowing the bridging behaviour to be dominated by a pullout mechanism. Under this mechanism, the fibre-bridging strength was strongly dependent on the frictional stress (τ_0); if τ_0 was low, the fibre-bridging stress (σ_0) would also be low (as reflected in **Eq. 2.3**). As a consequence, to maintain the margin between the maximum fibre-bridging strength (σ_0) and the matrix cracking strength (σ_c), high amount of fibre volume ($V_{f,crit}$) was needed when τ_0 was low. On the contrary, when the interfacial friction was high, the bridging strength was no longer governed by a pullout mechanism, but by a rupture mechanism. Accordingly, the maximum fibre bridging stress (σ_0) was mainly controlled by the fibre rupture strength (σ_{fu}) and became insensitive to τ_0 .

Based on the energy criteria (the dashed line in **Fig. 2.4 (a)**), a concave $V_{f,crit} - \tau_0$ was obtained. This profile implied that when τ_0 was too low or too high, it was more difficult to form steady state cracking in the composite, and hence the demand for fibres to satisfy the energy criteria was high. Yang & Li (2010) explained that this phenomenon occurred due to the change of the shape of $\sigma(\delta)$ curve with a change in τ_0 . When τ_0 was too low, the bridging stress shaping the $\sigma(\delta)$ profile became low, resulting in a small amount of complementary energy (J_b'). Similarly, when τ_0 was too high, the initial slope (i.e. the stiffness) of the $\sigma(\delta)$ curve increased, resulting in the decline of the complementary energy (J_b') as well. Consequently, both conditions impeded the formation of steady-state cracking.

For the example presented in **Fig. 2.4**, the optimum point to achieve strain hardening behaviour was attained when $V_{f,crit} = 3.5\%$ and $\tau_0 = 1.5 \text{ MPa}$. Nonetheless, since in practice the value of τ_0 arising from the sliding between a typical hydrophobic fibre (like PP fibre) and a cementitious matrix was only about 0.2-0.3 MPa (Kim, 1999), much higher fibre volume content (i.e. 7-13%) was required. There were several ways to improve the interfacial bonding properties of hydrophobic fibres, such as surface treatment by plasma (Li et al., 1996) or surface modification with sheath layer composition (Lhoneux et al., 2002; Yang & Li, 2010). However, even if τ_0 can be

improved up to 1.5 MPa, the minimum $V_{f,crit}$ value of 3.5% by volume was still deemed too high for optimum ECC design. Thus, other tailoring methods—for example by improving the fibre tensile strength (σ_{fu}), by adjusting the fibre length (L_f), or by adopting different fibre type—must be addressed.

Tailoring chemical bond strength (G_d)

When hydrophilic fibres such as polyvinyl-alcohol/PVA are used to reinforce a cementitious composite, the presence of chemical bond (in addition to frictional bond) at the fibre-matrix interface is expected. The influence of chemical bond strength (G_d) in governing the composite behaviour can be reviewed from the work by Lin et al. (1999), Redon et al. (2001), Li et al. (2002), and Yang & Li (2010). These past studies showed that the presence of chemical bond in addition to the frictional bond greatly influenced the prevailing bridging mechanism (i.e. pullout vs. rupture). The existence of high chemical bond was believed to increase the de-bonding threshold (in **Eq. 2.3**), promoting fibres-matrix interface to undergo rupture mechanism during the opening of a crack (Kanda & Li, 1998).

Lin et al. (1999) through numerical analysis investigated the effect of G_d on $\sigma(\delta)$ curve. The results of their analysis were reproduced here as **Fig. 2.5(a)** and **Fig. 2.5(b)** for (a) low tendency of fibre rupture and (b) high tendency of fibre rupture, respectively. In their studies, the bridging stress and the crack opening values were normalized to give non-dimensional $\sigma(\delta)$ curves and the parameter $\gamma/\tilde{L}_{c0}$ was used to represent the relative value of chemical bond strength to the frictional bond strength. As shown in **Fig. 2.5**, an increase of chemical bond strength would shift the initial point of $\sigma(\delta)$ upward, causing a decrease in the complementary energy (J_b'). Furthermore, it was also observed that as the chemical bond strength increased, the maximum bridging stress (σ_0) also increased. The effect was much more pronounced in the case with low tendency of fibre rupture (**Fig. 2.5(a)**).

Fig. 2.5 The effect of chemical bond in altering the $\sigma(\delta)$ curve (a) low tendency of fibre rupture (b) high tendency of fibre rupture (Lin et al., 1999)

Yang & Li (2010) using $V_{f,crit} - \tau_0$ curve approach as discussed in the previous section, also demonstrated ECC design optimization by tailoring the chemical bond strength. The results of their analysis are presented in **Fig. 2.6**. The effect of chemical bond strength on the critical fibre volume fraction was investigated by comparing two sets of parameters which were identical to each other except for the chemical bond strength (G_d) i.e. Gd5PVA ($G_d = 5 \text{ J/m}^2$) vs. Gd2PVA ($G_d = 2 \text{ J/m}^2$). It should be noted that the different values of matrix cracking strength (σ_c) and the maximum fibre bridging stress (σ_0) for Gd5PVA and Gd2PVA were allowed in this case due to the change in G_d (Li et al., 2002).

Fig. 2.6 The influence of frictional and chemical bond strength on the critical volume fibre fraction of a cementitious composite reinforced by PVA fibres (a) based on strength and energy criterion respectively (b) based on both criteria combined (Yang & Li, 2010)

The optimum point to achieve strain hardening behaviour in the case of Gd5PVA was at $V_{f,crit} = 3.5\%$ and $\tau_0 = 1.2 \text{ MPa}$, while that in the case of Gd2PVA was at $V_{f,crit} = 2.0\%$ and $\tau_0 = 2.0 \text{ MPa}$ (**Fig. 2.6(b)**). Hence, it was clear that the amount of PVA fibre required to obtain multiple cracking behaviour ($V_{f,crit}$) could be minimized effectively by lowering the chemical bond strength (G_d). This significant $V_{f,crit}$ reduction was primarily attributed to the increase in complementary energy (J_b') which created conducive condition for multiple cracking.

In practice, the chemical bond strength in fibre-matrix interface can be weakened by fibre surface coating using a hydrophobic oiling agent. Experimental investigations on this technique (Redon et al., 2001; Li et al., 2002) had been proven successful in alleviating the interfacial bond properties of PVA fibre, hence improving tensile ductility of the composite. By varying different content of oiling agent of PVA fibre, Li et al. (2002) observed that the oiling agent not only reduced the chemical bond strength (G_d), but also diminished the frictional bond strength (τ_0) and the slip hardening coefficient (β). The simultaneous reductions resulted in wider J_b'/J_{tip} margin as shown in **Table 2.2**.

Table 2.2 The effect of oiling agent on interfacial properties and complementary energy (Li et al., 2002)

oiling quantity (%by weight)	τ_0 (MPa)	G_d (J/m ²)	β	J_b' (J/m ²)	J_b'/J_{tip}
0	2.44 ± 0.49	4.71 ± 0.58	2.21 ± 0.71	3.64 - 6.63	0.73 - 1.33
0.3	2.15 ± 0.19	3.16 ± 0.66	2.31 ± 0.19	5.17 - 13.8	1.03 - 2.76
0.5	2.14 ± 0.15	2.96 ± 0.75	1.82 ± 0.23	7.68 - 13.6	1.54 - 2.72
0.8	1.98 ± 0.13	2.18 ± 0.39	1.18 ± 0.34	12.5 - 20.7	2.50 - 4.14
1.2	1.11 ± 0.13	1.61 ± 0.60	1.15 ± 0.17	24.2 - 38.1	4.84 - 7.62

(2) Tailoring matrix properties

Besides modifying the fibre-matrix interfacial properties, the design optimization of ECC can also be performed by tailoring their matrix properties. Li et al. (1995) showed that the crack tip toughness (J_{tip}) on the left hand side of **Eq. 2.1** was related to the matrix properties via **Eq. 2.9**. From this relationship, it is clear that tailoring the matrix mechanical properties accommodates the change of crack tip toughness (J_{tip}) such that it satisfies the energy criterion. In terms of the strength criterion, the matrix cracking

strength (σ_c) is governed by the matrix fracture toughness (K_m) and the flaw size distribution of the matrix (a_0) (Li et al., 2001; Li et al., 2002). In general, both the reduction of matrix toughness and the decrease of matrix cracking strength help ECC to satisfy the strain-hardening criterion more easily.

$$J_{tip} = K_m^2 / E_m \text{ (Eq. 2.9)}$$

Since the actual values of K_m and a_0 are highly dependent to the details of mixture composition of the matrix, it is important to address the influence of water/cement ratio, sand/cement ratio, sand size, and other relevant variables to the mechanical properties of the matrix. Several previous investigations of these issues are briefly summarized as follow.

The effect of w/c ratio

From the experimental results obtained by Li et al. (1995), it was observed in **Fig. 2.7** that a cementitious matrix that had lower w/c ratio (i.e. w/c = 0.35) generally had higher matrix tensile strength (σ_c) and toughness (K_m) compared to its counterpart that had higher w/c ratio (i.e. w/c = 0.45). Nonetheless, the discrepancies decreased as higher sand content was used. This finding seemed to suggest that decreasing w/c ratio increases both σ_c and J_{tip} , potentially hindering the composite to satisfy the strain-hardening criterion.

Fig. 2.7 The influence of water/cement and sand/cement ratios on (a) matrix tensile strength and (b) matrix fracture toughness (Li et al., 1995)

The effect of sand/cement ratio

Li et al. (1995) also investigated the effect of sand/cement ratio on the mechanical properties of the matrix (**Fig. 2.7**). Their results indicated that for a given w/c ratio an increase of sand content tended to increase K_m and σ_c . The strengthening and toughening of the matrix is attributable to an increase of energy required for the crack to propagate through more torturous path to avoid the sand grain. These effects resulted in the increase of both J_{tip} and σ_c , thus inhibiting ductile behaviour of the composite. Nonetheless, Li et al. (1995) also pointed out that the effect of high sand content on the matrix tensile strength was less prominent at low w/c ratio.

The influence of sand content on the composite behaviour was also studied by Akkaya et al. (2000). In agreement to Li et al. (1995), they found that the presence of sand decreased the overall ductility of the composite. Through micro-structural analysis, Akkaya et al. (2000) observed that the presence of sand in fibre-reinforced cementitious composites introduced high porosity in the matrix. This condition leads to an increase in matrix fracture toughness (K_m)—since the pores may act as crack arresters by “blunting” the crack tip (Mindess & Young, 1981). The increase of matrix toughness makes the energy criterion harder to achieve, hence reducing the overall ductility of the composite.

In another study, Li et al. (2001) found that an increase of the sand/cement ratio did not necessarily impede the development of ductile composite. They argued that for a composite consisting of only fine sand as aggregates (which is usually the case for ECC), the energy required to form a new crack surface might be diminished due to relatively weak bond between sand-cement particles (in comparison to the bond between cement-cement particles). Interestingly, Li et al. (2001) revealed that the sand/cement ratio also influenced the fibre-matrix interfacial frictional properties. A high sand content induced large interfacial frictional stress that might cause severe fibre abrasion leading to adverse bridging behaviour. Nonetheless, the use of oil coating agent would alleviate this effect.

Based on these studies, it can be concluded that the use of high sand content generally is not desirable for ductile composite design as it brings detrimental effects to both the matrix toughness and the fibre-matrix interfacial properties.

The effect of supplementary materials/mineral admixtures

It has been widely recognized that the use of supplementary materials, such as ground-granulated blast furnace slag (GGBS), fly ash, silica fume, or other similar industrial by-products offers several benefits to improve the performance of concrete. They play a role as secondary cementing materials (i.e. react with water to form CSH) and/or as micro-fillers (i.e. fill the voids between fine cement grains). The addition of supplementary materials generally results in the following outcomes (Neville, 1996):

- Increased composite density and strength
- Improved resistance against corrosion and alkali-aggregate reaction
- Decreased heat generation during hydration
- Reduced water demand
- Improved workability

Furthermore, the use of these materials in a large amount can also function as a partial replacement of ordinary Portland cement (OPC), which in turn allows a reduction in cement production cost and abatement of all negative environmental impacts (e.g. reduction of energy consumption and CO₂ emission).

The incorporation of high dosage of industrial waste in ECC had been investigated by Yang et al., (2007). In their study, class F fly ash was used as an OPC substitute (55-85% replacement by weight). The reason for using class F fly ash was due to its ease of processing and bountiful availability in the U.S.

From their observation using a scanning electron microscope (SEM), it was evident that the use of high volume of fly ash indeed increased the compactness of the matrix and the interfacial zone. It was believed that when high volume of fly ash was incorporated into the mixture, a large portion of fly ash particles remained un-hydrated and served as micro-fillers. The densification (as opposite to porosity when increasing sand content) reduced the matrix toughness, hence helped ECC to achieve the energy criterion. Moreover, Yang et al., (2007) found that adding high amount of fly ash not only affected the matrix properties, but also influenced the fibre-matrix interfacial properties. The densification by the un-hydrated fly ash particles at the interfacial zone increased the resistance of fibre sliding and led to an increase in frictional bond strength (τ_0), but reduced chemical bond strength (G_d). Overall, Yang et al., (2007) concluded that the

margin between J'_b and J_{tip} increased as the fly ash content increased as shown in **Fig. 2.8**. Therefore, ECC had a better chance to achieve multiple cracking behaviour with high fly ash content.

Fig. 2.8 The influence of fly ash/cement (FA/C) ratio on J'_b/J_{tip} margin (Yang et al., 2007)

The effect of flaw size distribution

According to Griffith's hypothesis (Griffith, 1921), failure of brittle materials is generally governed by microscopic flaws in the bulk material. Thus, the flaw size distribution played a significant role in determining the strength of a material. The higher the probability of a specimen containing large critical flaws, the lower would be the measured strength (Weibull, 1939; Griffith, 1921). In the case of ECC, the critical flaws can be in the form of voids/capillary pores formed during mixing and casting, bond cracks at fibre-matrix interfaces, and other discontinuities.

Li & Wang (2006) illustrated the relationship between the inherent flaw size and matrix cracking strength in ECC reinforced by 2% PVA fibre as shown in **Fig. 2.9**. It was clear from **Fig. 2.9** that there existed a minimum initial flaw size (c_{mc}) that should be attained for the strength criterion to be valid (i.e. **Eq. 2.2**). The flaws larger than c_{mc} was designated as active flaws (since they contributed to formation of multiple cracking), while those smaller than c_{mc} was termed as inert flaws (since they were abstained from participating in the formation of multiple cracking).

Fig. 2.9 The relationship between the inherent flaw size and matrix cracking strength in ECC reinforced by 2% PVA fibre (Li & Wang, 2006)

In a typical normal cast PVA-ECC mix, the size of pre-existing flaws was usually small—due to the absence of coarse aggregate—and their distribution was naturally wide-ranging (as described in **Fig. 2.10(a)**). To promote robust multiple cracking behaviour, Li & Wang (2006) proposed a practical approach to control pre-existing flaw size distribution by introducing artificial flaws with prescribed size distribution (**Fig. 2.10(b)**). The artificial flaws could be in the form of any particles that have low tensile strength and weak bonds to the matrix. In their study, Li & Wang (2006) adopted lightweight aggregates made from expanded shale as artificial flaws.

Fig. 2.10 The flaw size distribution (a) natural flaws (b) superimposed natural and artificial flaws (Wang & Li, 2004)

A comparison of uni-axial tension test results between ECC specimens with artificial flaws (a graded size of 3.5 ± 0.2 mm with a volume fraction of 7%) and those without artificial flaws revealed that the former exhibited better tensile ductility than the latter

(Fig. 2.11). Hence, it was evident that incorporating artificial flaws could boost the formation of multiple cracking.

Fig. 2.11 The uni-axial tensile stress-strain relationships (a) with artificial flaws (b) without artificial flaws (Li & Wang, 2006)

From the above discussion regarding tailoring of matrix properties, it can be concluded that reducing matrix toughness and matrix tensile strength generally enhances the tensile ductility of ECC. Nonetheless, it should be noted that this approach also resulted in a decrease of the first crack strength of composites, which may not be desirable for serviceability limit state. In addition, several approaches intended to tailor the matrix properties of the composite (e.g. by controlling the sand content or by adding supplementary materials) often affect interfacial properties of the composite as well. Thus, it is important to consider tailoring of the interface, matrix, and fibre as a unified system so that ECC composite design can be truly optimized.

(3) Tailoring of fibre properties

Another alternative design approach to optimize ECC design is by tailoring the fibre properties, viz. fibre ultimate tensile strength (σ_{fu}) and fibre length (L_f). This approach is normally adopted for ECC with PP fibre since it was characterized by low tensile strength and weak interfacial bond with cementitious matrix.

The effect of fibre ultimate tensile strength (σ_{fu})

To understand the effect of σ_{fu} on the behaviour of ECC, Yang & Li's (2010) $V_{f,crit} - \tau_0$ curves derived from two sets of parameters—identical to each other except for the value

of σ_{fu} —were compared. An increase of the ultimate tensile strength (σ_{fu}) obviously enhances the load-carrying capacity of each individual fibre participating in the bridging action. Hence, it is expected that to attain equivalent bridging behaviour, less amount of fibre is needed with stronger fibres. Indeed, it can be observed from **Fig. 2.12** that by adopting higher σ_{fu} , the minimum fibre content necessary to fulfil the strength and energy criteria ($V_{f,crit}$) decreases at all τ_0 . Nonetheless, a significant reduction is particularly observed at high friction zone ($\tau_0 > 0.3MPa$). This result implied that to fully utilize the benefit of using enhanced fibre strength, it was necessary to increase the interfacial friction (τ_0) as well. A successful optimization of ECC using high strength/tenacity fibre with improved interfacial bond had been demonstrated by Lhoneux et al. (2002) for PP-ECC (i.e. ECC reinforced by PP fibre).

The effect of fibre length (L_f)

The prevalent influence of fibre length can be explained in terms of the stress transfer (i.e. bridging) mechanism during fibre pullout. When longer fibres are adopted, there will be a higher chance for the fibres at the crack plane to have sufficient embedment length necessary to achieve full tensile strength capacity. Thus, with longer fibres, the stress transfer mechanism contributed by each fibre is expected to mobilize more effectively. Consequently, the amount of fibres required to hold the crack plane can be reduced. The comparison of $V_{f,crit} - \tau_0$ curves (**Fig. 2.13**) for fibres with different lengths derived by Yang & Li (2010) was in agreement with this notion; an increase of L_f reduced $V_{f,crit}$. In contrast to the effect of σ_{fu} discussed in the previous section, the reduction of $V_{f,crit}$ was more prominent at low friction zone ($\tau_0 < 1 MPa$). Thus, this tailoring method was more suitable for fibres with low interfacial friction, such as PP fibres. Nonetheless, it should be noted that an increase of fibre length may cause adverse effects such as poor fibre dispersion, poor workability, and fibre rupture (Banthia et al., 1997).

Fig. 2.12 The influences of σ_{fu} and τ_0 on $V_{f,crit}$ of PP-ECC (a) based on strength and energy criterion respectively (b) based on both criteria combined (Yang & Li, 2010)

Fig. 2.13 The influences of l_f and τ_0 on $V_{f,crit}$ of PP-ECC (a) based on strength and energy criterion respectively (b) based on both criteria combined (Yang & Li, 2010)

By applying the micromechanical tools discussed above, the minimum fibre volume fraction just sufficient to obtain ECC with ductile behaviour can be efficiently determined. It is emphasized that to develop fully optimized ECC, tailoring of interfacial properties, matrix properties, and fibre properties must be considered as a holistic design approach. Thus, each modification on the ECC constituent materials must be assessed by considering all these interactions.

2.6. ECC tensile behaviour under high-strain rate

The ability of ECC to achieve high energy absorption capacity and high damage tolerance, undoubtedly suggests that there is huge potential for its application in structures subjected to extreme loading conditions, such as earthquake, impact, and blast. Nonetheless, it should be noted these promising ideas were established based on ECC behaviour under quasi-static conditions; there is no guarantee that ECC retains its superior mechanical performances under high strain rate loading.

Several studies investigating the rate-dependency of ECC tensile behaviour had been conducted by previous authors (Yang & Li, 2005; Douglass & Billington, 2005; Maalej et al., 2005; Mechtcherine et al., 2011, Caverzan et al., 2012). Generally, all of the authors agreed that the tensile strength of ECC was strain-rate dependent; it increased as the strain-rate increased. In contrast, a disagreement between authors arose regarding the strain-rate effect on the energy absorption capacity of ECC. At medium range of strain rates (static to earthquake loading case, i.e. 10^{-5} to 0.1 s^{-1}), Yang & Li (2005) and Douglas & Billington (2005) found that the energy absorption capacity of ECC diminished at moderate strain rate, while Maalej et al. (2005) observed that the energy absorption capacity of ECC was maintained at higher strain rate. Interestingly, at higher range of strain rates (impact loading case, i.e. $150\text{-}300 \text{ s}^{-1}$), Mechterine et al. (2011) and Caverzan et al. (2012) reported that the energy absorption capacity of ECC was enhanced at high strain rate, indicating a positive influence of increased strain rates on ECC performance. The disagreement between authors might stem from numerous reasons, such as variations in mix design, test methods, age, size and shape of the specimens as summarized in **Table 2.3**. Nonetheless, Yang & Li (2005; 2012) indicated that the discrepancies due to variations in mix design could be addressed through micromechanics principle.

Yang & Li (2005; 2012) highlighted that even at high strain rate, the synergistic interaction between matrix and fibres still played a key role in determining the energy absorption capacity of ECC. Therefore, similar to the quasi-static case, as long as the energy criterion (**Eq. 2.1**) and the strength criterion (**Eq. 2.2**) are satisfied, large energy dissipation through formation of multiple micro-cracking can be retained. However, it should be noted that each micromechanical parameter governing matrix toughness and fibre-bridging law (as listed in **Table 2.1**) may be strain-rate dependent. As the strain-

rate increases, the values of these micro-mechanical parameters may change, leading to either favourable or unfavourable conditions for multiple cracking.

Table 2.3 Past studies on ECC tensile behaviour under dynamic conditions

Author(s)	Types of fibre (% Vol)	Mix proportion									Strain rate (s ⁻¹)	Test Method
		Cement	Fly Ash	slag	Silica Fume	Water	Sand	SP ¹	VMA ¹	SAP ¹		
Yang & Li (2005)	PVA (2%)	1.00	1.20	-	-	0.53	0.80	0.030	-	-	10 ⁻⁵ – 0.1	servo-hydraulic machine
Douglass & Billington (2005)	PVA (2%)	1.00	0.43	-	-	0.56	0.71	0.003	0.003	-	2x10 ⁻⁵ – 0.2	servo-hydraulic machine
Maalej <i>et al.</i> (2005)	PE (1.5%) + steel fibres (0.5%)	1.00	-	-	0.10	0.28	-	0.020	-	-	2x10 ⁻⁶ – 0.2	servo-hydraulic machine
Kim <i>et al.</i> (2009)	steel fibres (1-2%)	0.70	0.30	-	-	0.65	3.50	0.009	0.002	-	10 ⁻⁴ – 0.1	servo-hydraulic machine
		1.00	0.15	-	-	0.35	1.00	0.009	0.006	-		
		0.80	0.20	-	0.07	0.26	1.00	0.040	0.012	-		
Mechtcherine <i>et al.</i> (2011)	PVA (2.2%)	1.00	1.20	-	-	0.66	1.06	0.019	0.006	0.004	10 ⁻³ – 180	Hopkinson bar
Caverzan <i>et al.</i> (2012)	steel fibres (1.25%)	1.00	-	0.83	-	0.33	1.64	0.063	-	-	0.1 – 300	Hopkinson bar

¹ SP = Superplasticizer, VMA = Viscosity Modifying Admixtures, SAP = Super-Absorbent Polymers

To identify the rate dependencies of the fibre-matrix interface properties of PVA-ECC, Yang & Li (2005) conducted single fibre pullout tests using hydraulic machine at varying pullout speeds that range from 10⁻³ to 10 mm/s. Three interfacial parameters were investigated: chemical bond strength (G_d), frictional bond strength (τ_0), and slip hardening coefficient (β). They discovered that frictional bond strength and slip hardening coefficient were rate-independent, while chemical bond strength was strongly rate-dependent. As the strain rate increased, the chemical bond strength also increased; the chemical bond strength at the fastest pullout speed was equal to 5 times that at quasi-static case. The increase of chemical bond, as discussed earlier, promotes fibre rupture, decreases J_b' , and increases σ_0 —all of these actions in turn may lead to a violation of the strain-hardening criteria.

To ensure strain-hardening behaviour of ECC even at high strain rate, Yang & Li (2005; 2012) further recommended the following approaches:

- increasing pozzolan content (i.e. industrial by products such as fly ash) in the matrix to minimize hydration at the interfacial transition zone to reduce chemical bond and to lower matrix toughness
- using hydrophobic fibres (e.g. PE) or hydrophilic fibres (e.g. PVA) with hydrophobic oiling coat
- using fibres with short length (e.g. 8 mm length) to prevent fibre rupture
- incorporating light weight aggregates to control the flaw size distributions

Hence, it is clear that the tailoring design method for ECC under high strain-rate is actually identical to that under quasi-static case although the conditions to maintain large J'_b/J_{tip} and σ_0/σ_c margins may be more difficult to attain.

Unfortunately, due to limited capability of the present equipment (typical hydraulic machine for a single fibre pullout tests can only achieve a maximum strain rate of 0.1s⁻¹), it is difficult to quantify the micromechanical properties governing the strain-hardening behaviour of the composite at strain rate beyond 0.1s⁻¹. Thus, although the micromechanical model is theoretically valid for high strain rate condition, the reasonable values of parameters to be substituted into the model cannot be ascertained in the laboratory.

In addition, conducting uni-axial tensile tests to obtain a stress-strain relationship of cementitious composite especially at high strain rates is technically very difficult. Most high strain rate tensile testing experiments on cementitious composites utilized impact loading that generate tensile stress waves by reflecting the compressive stress waves (i.e. indirect tensile loading method), e.g. spalling SHPB tests (Klepaczko & Brara, 2001; Mechterine et al., 2011) or Brazilian splitting SHPB tests (Ross et al., 1989; Lok et al., 2003). Unfortunately, these experimental techniques suffer a major drawback—they are unable to produce complete stress-strain relationships of the test materials; only tensile strength, young's modulus, and fracture energy can be obtained from these techniques. To overcome this limitation, Kim et al. (2011) proposed a new test system named SEITS (strain energy impact test systems), which was later improved and modified by Tran & Kim (2012) into SEFIM (strain energy frame impact machine). The new system is a

promising tool for dynamic tensile testing of high performance cementitious composites such as ECC, but unfortunately it is not yet available in many laboratories.

2.7. ECC compressive behaviour under static and high-strain rate conditions

A comparison of mechanical properties of normal concrete, high-strength concrete, and ECC under quasi-static compression is shown in **Table 2.4**. It is recognized that the compressive strength of ECC is not notably difference from normal concrete (NC) or high-strength concrete (HSC). The elastic modulus of ECC is the lowest among the other categories due to the absence of coarse aggregate. But in terms of ultimate strain capacity, ECC shows the best performance. Similar to other fibre-reinforced cementitious composites, the post-peak behaviour of ECC also decreases more gradually than that of normal or high-strength concrete as described by **Fig. 2.14** (Nawy, 2008; fib, 2013). Nonetheless, the improvement of mechanical properties under quasi-static compression reckoned by adopting ECC is considered insignificant (as compared to that under quasi-static tension). This fact explains why the researchers focus more on tensile performance of ECC.

Table 2.4 Comparison of compressive mechanical properties between NC, HSC and ECC under quasi-static conditions

Cementitious composites	Cylinder compressive strength (MPa)	Ultimate strain	Elastic Modulus (GPa)
NC (fib, 2013)	12-50	0.0034-0.0035	27-39
HSC (fib, 2013)	50-120	0.0030-0.0034	40-50
ECC (Nawy, 2008)	20-95	0.0045-0.0065	18-34

For impact and blast applications, however, it is important to comprehend the compressive behaviour of ECC under high strain rate. This is simply because when impact or blast loads attack a target structure, the initial stress imposed on the target is in the form of compressive stress wave. Hence, if the target structure has insufficient compressive deformation capacity within a short duration, it may be severely damaged at the initial stage—even before the incident waves are reflected as tensile stress waves. Moreover, it is interesting to note that failure of cementitious composites in compression is still governed by formation of tensile cracking according to Griffith hypothesis (Neville,

1996; van Mier, 1998). Thus, the behaviour of ECC in compression can actually be correlated to its behaviour in tension, and vice versa. Unfortunately, ECC compressive performance under rapid strain rate has not been researched extensively. Thus, there is a need to further investigate this matter.

Fig. 2.14 comparison of post-peak behaviour between normal concrete and fibre-reinforced composite (fib, 2013)

Recently, an interesting discovery on dynamic behaviour of ECC with a high volume of ground-granulated blast furnace slag (GGBS) content was reported by Chen et al. (2013). They observed that at strain-rate in the range of $85\text{-}180\text{s}^{-1}$, ECC with a high volume of GGBS exhibited compressive strain-hardening behaviour as shown in **Fig. 2.15**. Overall, the compressive behaviour of ECC was also rate-dependent; as the strain rate increased, the ultimate strength increased, while the strain capacity decreased.

Chen et al. (2013) hypothesized that the behaviour was probably due to simultaneous fracture of cementitious matrix at many sites under dynamic conditions providing numerous crack-initiation sources that could activate fibre bridging mechanism. The presence of high GGBS content in the composite helped to decrease the matrix toughness (hence decrease J_{tip}) and reduced the interfacial fibre-matrix bond (hence prevent fibre rupture). Both actions encouraged the formation of multiple-cracking behaviour. Their pioneering work laid a foundation for developing ECC materials that had superior compressive performance under dynamic condition. Nonetheless, since the range of strain-rate investigated in this study was limited, further investigations for higher range of strain-rate (i.e. $100\text{-}1000\text{s}^{-1}$) were necessary.

Fig. 2.15 Dynamic compressive strain-hardening behaviour of GGBS-ECC (a) 70% cement replacement by GGBS (b) 80% cement replacement by GGBS (Chen et al., 2013)

2.8. Summary

In this chapter, general descriptions of ECC and its mechanical behaviour were presented. It was emphasized that the ability of ECC to achieve a considerable degree of tensile strain and ductility—despite having relatively lower fibre content than normal HPFRCC—relied on the synergistic interactions between the fibre and the matrix described by micromechanics principle.

The superior performances of ECC indicated that there is great potential for its applications on structures requiring high energy absorption capacity and high damage tolerance, such as national critical infrastructures (e.g. government buildings, embassies, military facilities) that are expected to withstand extreme loading conditions such as impact and blast. Past research studies, however, showed that the energy absorption capacity of ECC might be compromised under dynamic events. Hence, stringent measures should be taken into consideration for designing ECC subjected to high rate loading. Unfortunately, conducting uni-axial tensile tests at high strain-rate was a challenging task due to limitation of present experimental tensile testing method to generate a complete dynamic stress-strain relationship.

On the other hand, the compressive behaviour of ECC had been observed mostly under quasi-static tests; the performance of ECC under dynamic compression is generally still vague. Therefore, there is an urgency to conduct further study to characterize the response of ECC under high strain rate.

CHAPTER 3: LITERATURE REVIEW ON SPLIT-HOPKINSON PRESSURE BAR (SHPB) TESTS

3.1. Introduction

The Split Hopkinson Pressure Bar (SHPB) or Kolsky Bar is an apparatus commonly used for determining mechanical properties of materials at high strain rate. The underlying principle behind the high strain rate testing using SHPB apparatus includes the elastic wave propagation and wave interaction theories. The SHPB test was originally developed to extract mechanical properties of ductile materials (e.g. metals) and soft materials (e.g. rubber and plastics) under dynamic compression. Over the years, however, the apparatus has been modified for testing brittle materials (e.g. concrete, ceramics, and rocks) as well. Later inventions also allow the use of SHPB apparatus to study the mechanical properties of materials subject to tension, shear or torsion in addition to compression.

3.2. Brief historical background

The Hopkinson pressure bar (HPB) is named after Bertram Hopkinson (1914), a professor of Mechanism and Applied Mechanics at Cambridge University, who invented a way to measure the magnitude and duration of stress pulses excited during dynamic events in a long elastic metallic bar. Based on Hopkinson's pioneering works, Davies (1948) improved the apparatus—by employing electrical measuring technique—such that it could be used to measure both axial and radial strain in HPB, giving more accurate stress pulse measurements and obtaining the stress-time relationship. Nonetheless, it was not until Kolsky (1949) published his research work that the pressure bar apparatus was (and still is) the most widely used device to determine dynamic mechanical properties of materials.

Kolsky (1949) modified the Davies' HPB configuration by splitting the pressure bar into two parts—"the main bar" and "the extensions" or in the present terminology, "the incident/input bar" and "the transmitted/output bar"—and inserting disk specimens in between these bars. He also derived the equations to obtain stress-strain relationships of tested materials, discussed the effect of radial inertia on the accuracy of test results, and examined the relaxation behaviour under rapid loading conditions. Since then,

Kolsky bar or split-Hopkinson pressure bar (SHPB) apparatus has been utilized to study dynamic mechanical properties of various materials.

3.3. Typical configuration of SHPB apparatus

A typical SHPB system is illustrated in **Fig. 3.1**. It comprises two main parts: impact component and signal-processing component.

Fig. 3.1 Typical Configuration of SHPB Apparatus

The impact component consists of the following:

- A gas chamber as a propulsion device to launch the striker bar
- A striker bar and two long pressure bars (i.e. an incident bar and a transmitter bar) made of the same materials and of uniform cross section
- Bearings and alignment fixtures to allow the bars to move freely yet keeping their proper axial alignment
- A stopper as a momentum trap

The signal-processing component consists of the following:

- Strain gauges attached at the surface of both bars to measure the stress wave propagation in the bars
- A strain meter (or a signal amplifier) to magnify weak electronic signals induced by the strain pulse
- A digital oscilloscope to transform electronic signals into digital signals

- A computer to process and analyse the recorded signals

The pressure bars were typically made of high-strength, high-elastic modulus metals, such as maraging steel or nickel alloy ($E=205\text{--}210\text{ GPa}$) to ensure that the bars would remain elastic at any given stress during the tests. Nonetheless, it should be noted that the use of high strength, high elastic modulus metal bars for testing low strength, low impedance materials (i.e. soft materials, such as rubber, elastomers, and glassy polymers) would reduce signal-to-noise level. This would compromise the sensitivity and the accuracy of recorded pulses (Chen *et al.*, 1999). Therefore, the final selection of bar materials depended mainly on the strength and the mechanical impedance of the test materials. In practice, researchers have been using a wide range of bar materials: maraging steel ($E=210\text{ GPa}$), titanium ($E=110\text{ GPa}$), aluminium ($E=90\text{ GPa}$), magnesium ($E=45\text{ GPa}$), and polymeric materials ($E<20\text{ GPa}$) (Gray III, 2000).

3.4. Theory of SHPB tests

3.4.1. One-dimensional elastic stress wave theory

When a striker hit a long cylindrical bar, a compressive stress wave will be generated and it will propagate into the striker and the long bar in the direction away from the impact surface as shown in **Fig. 3.2**.

Fig. 3.2 Initiation and propagation of compressive stress wave due to bar impact (a) prior to impact (b) post impact

The wave propagation velocity can be calculated from Newton's second law of motion on an infinitesimal body (**Fig. 3.3**).

$$F = ma$$

$$-A\sigma + A\left(\sigma + \frac{\partial\sigma}{\partial x}\delta x\right) = (A\rho\delta x)\frac{\partial^2 u}{\partial t^2}$$

$$\frac{\partial\sigma}{\partial x} = \rho\frac{\partial^2 u}{\partial t^2} \quad (\text{Eq. 3.1})$$

Fig. 3.3 An infinitesimal body subjected to compressive stress wave

For small and elastic deformation, the stress can be defined as follows:

$$\sigma = E\varepsilon \quad (\text{Hooke's law})$$

$$\sigma = E\frac{\partial u}{\partial x} \quad (\text{for small 1D deformation, } \varepsilon = \frac{\partial u}{\partial x})$$

Substitute it to **Eq. 3.1** and re-arrange the equation, we obtain

$$\frac{\partial}{\partial x}\left[E\frac{\partial u}{\partial x}\right] = \rho\frac{\partial^2 u}{\partial t^2}$$

$$\frac{\partial^2 u}{\partial t^2} = \frac{E}{\rho}\frac{\partial^2 u}{\partial x^2} \quad (\text{Eq. 3.2})$$

Eq. 3.2 is a very important equation describing the propagation of elastic waves in longitudinal direction. If the particles in the bar vibrate with simple harmonic motion, the displacement (u) as a function of position (x) and time (t) can be expressed as:

$$u = u_o \sin\left[2\pi f\left(t - \frac{x}{c_o}\right)\right] \quad (\text{Eq. 3.3})$$

Differentiate **Eq. 3.3** twice with respect to x and t and substitute it into **Eq. 3.2**, we can determine the velocity of the elastic wave (C_o).

$$\begin{aligned}\frac{\partial^2 u}{\partial x^2} &= -u_o \left(\frac{4\pi^2 f^2}{C_o^2} \right) \sin 2\pi f \left(t - \frac{x}{C_o} \right) \\ \frac{\partial^2 u}{\partial t^2} &= -u_o (4\pi^2 f^2) \sin 2\pi f \left(t - \frac{x}{C_o} \right) \\ -u_o (4\pi^2 f^2) \sin 2\pi f \left(t - \frac{x}{C_o} \right) &= \frac{E}{\rho} \left[-u_o \left(\frac{4\pi^2 f^2}{C_o^2} \right) \sin 2\pi f \left(t - \frac{x}{C_o} \right) \right] \\ \therefore C_o &= \sqrt{\frac{E}{\rho}} \quad (\text{Eq. 3.4})\end{aligned}$$

Thus, **Eq. 3.2** can also be expressed as,

$$\frac{\partial^2 u}{\partial t^2} = C_o^2 \frac{\partial^2 u}{\partial x^2} \quad (\text{Eq. 3.5})$$

By simple substitution (Meyers, 1994), it can be shown that the general solution of the above second-order partial differential equation for any general waves—either harmonic or non-harmonic pulse—is

$$u(x, t) = F(x - C_o t) + G(x + C_o t) \quad (\text{Eq. 3.6})$$

where F and G are arbitrary functions.

The first term in **Eq. 3.6** can be thought of as a wave of arbitrary shape (described mathematically by function F) propagating in positive x -direction at velocity C_o , while the second term can be thought of as a wave of arbitrary shape (described mathematically by function G) propagating in negative x -direction at velocity C_o . Thus, if the wave propagates only in one direction, the equation will consist of only one term. Furthermore, it is interesting to note that the shapes of the waves do not change as they propagate along the axis. For instance, assuming that the wave propagates only in positive x -direction and the time history of displacement at location P is known, one can calculate the time history of displacement at location Q, Δx away from P, as follows.

$$\text{Displacement at P, at time } t : \quad u(x, t) = F(x - C_o t)$$

$$\text{Displacement at Q, at time } t : \quad u(x + \Delta x, t) = F(x + \Delta x - C_o t)$$

Since the wave propagates along the x -axis with velocity C_o , the time taken for the wave to travel from location P to Q can be calculated as $\Delta x / C_o$.

Displacement at Q, at time $t + \frac{\Delta x}{C_o}$:

$$u\left(x + \Delta x, t + \frac{\Delta x}{C_o}\right) = F\left(x + \Delta x - C_o\left(t + \frac{\Delta x}{C_o}\right)\right) = F(x - C_o t)$$

Thus, it can be concluded that the displacement experienced at location Q will be the same as the displacement at location P, but delayed by a time interval of $\Delta x / C_o$. **Fig. 3.4** shows a schematic diagram of elastic wave travelling in positive x -direction. The same concept applies to the second term of **Eq. 3.6** which travels in negative x -direction.

Fig. 3.4 Elastic wave travelling in positive x -direction (a) at time t (b) at time $t + \frac{\Delta x}{C_o}$

By taking derivative of **Eq. 3.6** with respect to time (i.e. $\frac{\partial u}{\partial t}$) and coordinate x (i.e. $\frac{\partial u}{\partial x}$), one can obtain particle velocity ($\dot{u}$) and axial strain (ε) of the pressure bars, respectively.

Table 3.1 summarizes the derivation results for waves propagating in either positive or negative direction.

Table 3.1 Particle displacement, particle velocity and axial strain of the pressure bars due to 1-D wave propagation in positive and negative directions

General Solution of 1-D wave equation	Wave propagating in positive x -direction	Wave propagating in negative x -direction
Particle displacement: $u(x, t)$	$F(x - C_o t)$ (Eq. 3.6a)	$G(x + C_o t)$ (Eq. 3.6b)
Particle velocity: $v(x, t) = \frac{\partial u}{\partial t}$	$-C_o F'(x - C_o t)$ (Eq. 3.7a)	$C_o G'(x + C_o t)$ (Eq. 3.7b)
Axial strain: $\varepsilon(x, t) = \frac{\partial u}{\partial x}$	$F'(x - C_o t)$ (Eq. 3.8a)	$G'(x + C_o t)$ (Eq. 3.8b)

As shown in **Eq. 3.7** and **Eq. 3.8**, particle velocity and axial strain propagate at the same velocity as the particle displacement and their profiles remain unchanged as the wave propagates along the bar. Thus, similar to particle displacement, particle velocity and axial strain profiles at an arbitrary location on the bar (e.g. location **P** in **Fig. 3.4**) will be the same as those at other locations on the bar (e.g. location **Q** in **Fig. 3.4**), although they are delayed by some time interval (i.e. $\Delta x / C_o$). These findings explain why the strain gauges installed on the mid-span of the pressure bars in SHPB apparatus can be used to describe the strain profile at the bar-specimen interfaces.

Furthermore, the relationship between particle velocity and axial strain can be established by eliminating F' and G' from **Eq. 3.7** and **Eq. 3.8**, respectively.

For wave propagating in **positive** x -direction: $v = -C_o \varepsilon$ (Eq. 3.9a)

For wave propagating in **negative** x -direction: $v = C_o \varepsilon$ (Eq. 3.9b)

3.4.2. Propagation of elastic wave in SHPB tests

The impact of the striker bar on the input bar in SHPB compression tests generates a compressive wave that will travel along the input bar towards the specimen. This impact-generated compressive wave is referred to as the **incident pulse** and its amplitude is measured by the strain gauges located at the mid-point of the input bar. Upon reaching the input bar-specimen interface, the pulse will be partly reflected back to the input bar and will be partly transmitted to the specimen. Similarly, when the wave reaches the

specimen-output bar interface, the wave will be partly reflected back to the specimen and will be partly transmitted to the output bar. Thus, the wave continues to undergo multiple back and forth reflections within the specimen. The part of pulse that is reflected back to the input bar is referred to as the **reflected pulse** and its amplitude is measured by the strain gauges on the input bar. It should be noted that the input bar should be sufficiently long so that the incident wave is able to complete itself before the reflected wave reaches the strain gauge; the incident and the reflected waves will not overlap each other. The part of pulse that is transmitted forward to the output bar is referred to as the **transmitted pulse** and its amplitude is measured by strain gauges on the output bar. **Fig. 3.5** describes the stress wave propagation in SHPB tests with *Lagrangian* (i.e. time-distance) diagram.

Fig. 3.5 Lagrangian diagram of stress wave propagation in SHPB tests (Ramesh, 2008)

Assuming that the wave propagation in SHPB tests follows uni-axial one-dimensional elastic wave theory described in **Section 3.4.1**, the particle velocities due to incident (v_i), reflected (v_r), and transmitted (v_t) waves are given by:

$$v_i = -C_o \varepsilon_i \quad v_r = C_o \varepsilon_r \quad v_t = -C_o \varepsilon_t$$

where C_o is the elastic wave propagation velocity in pressure bars, ε_i , ε_r , and ε_t is the incident, reflected, and transmitted strain waves, respectively.

The incident and the transmitted waves are propagating in positive direction, while the reflected wave is propagating in negative direction. Hence, particle velocities at the specimen-bar interfaces (**Fig. 3.6**) are described as follow.

Particle velocity at input bar-specimen interface, v_1 : $v_1 = v_i + v_r = -C_o \varepsilon_i + C_o \varepsilon_r$

Particle velocity at specimen-output bar interface, v_2 : $v_2 = v_t = -C_o \varepsilon_t$

Fig. 3.6 Interaction of incident, reflected and transmitted waves at specimen-bar interfaces

The strain rate of the specimen ($\dot{\varepsilon}_s$) with length l_s can be defined as

$$\dot{\varepsilon}_s = \frac{v_2 - v_1}{l_s} = \frac{C_o}{l_s} (\varepsilon_i - \varepsilon_r - \varepsilon_t) \quad (\text{Eq. 3.10})$$

Integrating **Eq. 3.10**, one can obtain the axial strain history of the specimen (ε_s).

$$\varepsilon_s = \int_0^t \dot{\varepsilon}_s dt = \frac{C_o}{l_s} \int_0^t (\varepsilon_i - \varepsilon_r - \varepsilon_t) dt \quad (\text{Eq. 3.11})$$

The axial stress in the specimen can be determined by taking average forces at the two interfaces and dividing it by the cross sectional area of the specimen, A_s .

Force in input bar-specimen interface, F_1 : $F_1 = AE(\varepsilon_i + \varepsilon_r)$

Force in specimen-output bar interface, F_2 : $F_2 = AE \varepsilon_t$

Average stress in the specimen (σ_s):

$$\sigma_s = \frac{F_1 + F_2}{2A_s} = \frac{AE}{2A_s} (\varepsilon_i + \varepsilon_r + \varepsilon_t) \quad (\text{Eq. 3.12})$$

where A and E are the cross sectional area and the elastic modulus of the pressure bar, respectively. Most, if not all, SHPB apparatuses have input and output bars made of identical material and have the same cross sectional area.

Davies & Hunter (1963) observed that dynamic stress equilibrium in a specimen could be achieved after several reverberations. Thus, it can be assumed that the forces at the two interfaces are equal (i.e. $F_1 = F_2$, and hence $\varepsilon_i + \varepsilon_r = \varepsilon_t$). If this assumption is valid, **Eq. 3.10**, **Eq. 3.11** and **Eq. 3.12** can be simplified further as shown below.

$$\dot{\varepsilon}_s = \frac{2C_0}{l_s} \varepsilon_r \quad (\text{Eq. 3.13})$$

$$\varepsilon_s = \frac{2C_0}{l_s} \int_0^t \varepsilon_r dt \quad (\text{Eq. 3.14})$$

$$\sigma_s = \frac{AE}{A_s} \varepsilon_t \quad (\text{Eq. 3.15})$$

The stress-strain analysis using **Eq. 3.10-Eq. 3.12** is commonly known as *3-wave analysis* since it involves all the three waves in the calculation, while that using **Eq. 3.13-Eq. 3.15** is commonly known as *1-wave analysis* since it only includes reflected wave to calculate strain and transmitted wave to calculate stress.

Since the stress-state equilibrium condition is difficult to achieve at high strain-rate, it is theoretically more reasonable to adopt the 3-wave analysis method—which takes into account the average stresses across the length of the specimen. Unfortunately, summation of unsynchronized waveforms during the averaging process may potentially generate high-frequency oscillations which eventually lead to uncertainties in the interpretation of the test results. For this reason, the 1-wave analysis method is more popular than the 3-wave analysis counterpart. In the present study, the 1-wave analysis method will be adopted to derive the stress-strain relationship of the test specimens.

3.4.3. Data processing in SHPB tests

Fig. 3.7 shows typical recorded signals obtained from SHPB tests. The abscissa and the ordinate are time and voltage, respectively. A pair of strain gauges installed on the input bar measured both the incident and the reflected signals (solid line in **Fig. 3.7**), while a pair of strain gauges installed on the output bar measured only the transmitted signals (dashed line in **Fig. 3.7**). Since the incident and the reflected waves were measured by the same pair of strain gauges, the position of these strain gauges in the input bar should

be determined carefully so that the two waves would not overlap each other. In practice, the strain gauges were located at the mid-span of the pressure bars.

Fig. 3.7 Typical wave signals from SHPB tests (Gray III, 2000)

The first step to process raw data in SHPB test was to determine the initial starting point of each signal. The determination of initial starting point was important since it would affect the stress and strain measurements in the specimen. Although the task seems simple and easy at first, the presence of oscillations due to dispersion effect (**Section 3.5**), radial inertia, or environmental noise will disturb the original signal, hence making determination of initial starting point difficult. Past authors (Follansbee & Frantz, 1983; Zhao & Gary, 1996; Li & Lambros, 1999) suggested ways to estimate the initial starting point of the incident wave as summarized below.

The two most common methods to determine the initial starting point were visual inspection and calculation of stress-wave travel times. The visual inspection method was straightforward, but very subjective. On the other hand, the method by calculating stress wave travel times was more objective, although there were uncertainties due to dispersion effect and imperfect contact conditions at bar-specimen interfaces. The presence of a thin layer of grease or a rough contact surface would affect the accuracy of this method (Zhao & Gary, 1996; Zhao, 2003). The calculation of stress wave travel time (t) was based on the wave velocity in the pressure bars (C_0) and the distance between the positions of strain gauges and the specimen-bar interfaces (Δx), i.e. $t = \frac{\Delta x}{C_0}$.

Zhao & Gary (1996) proposed an iterative method using numerical simulations. This method was based on two assumptions: 1) matching the shape of the pulse is more reliable than time-shifting the initial starting point of the pulse, 2) at the early stage when

the pulse started loading the specimen, the test material would exhibit quasi-static behaviour. Although the method might give more objective solution, in practice it was complex and time-consuming.

Li & Lambros (1999) used “sampling window” method, whereby the slopes of successive points in the recorded signals within certain time window were investigated. If within a chosen window 90% of the measured slopes are negative (i.e. in compression), the starting point of this window could be taken as the starting point. Otherwise, this window was shifted in time until the above condition was satisfied. The accuracy of this method relied on the number of sampling points (i.e. the time window) chosen and the sampling rate used. The presence of noise during the tests would also affect the reliability of this method. Thus, in effect, the method was subjective.

After the initial starting point had been determined, the recorded stress wave signals would be separated into three individual signals (i.e. incident, reflected, and transmitted wave). Since it was assumed that the impact-generated stress waves in SHPB test followed one-dimensional elastic stress wave theory (**Section 3.4.1**), the separation into three components could be easily done by time-shifting the wave signals as shown in **Fig. 3.8**. The incident signal was shifted forward, while the reflected and the transmitted signals were transmitted backward. It should also be noted that the wave velocity in the specimen should be estimated when shifting the transmitted pulse.

Fig. 3.8 Time-shifting of incident, reflected and transmitted pulses

Hence, the material stress-strain relationship could be obtained by applying **Eq. 3.10-Eq. 3.12** or **Eq. 3.13-Eq. 3.15** derived in **Section 3.4.2**.

3.4.4. Characteristic of incident wave in compression SHPB tests

The impact between the striker bar (with initial velocity v_o) and the input bar (initially at rest) generates a compressive incident pulse that propagates in the direction away from the impact surface as depicted in **Fig. 3.2** and **Fig. 3.9**; the pulse propagates leftward in the striker bar, but propagates rightward in the incident bar. If one-dimensional elastic wave propagation is assumed, the particle velocities of the striker bar (subscript “A”) and the input bar (subscript “B”) after impact can be described as follows.

$$v_A = C_{o,A} \varepsilon_A \quad (\text{Eq. 3.16})$$

$$v_B = -C_{o,B} \varepsilon_B \quad (\text{Eq. 3.17})$$

Fig. 3.9 Continuity and force equilibrium at the impact face

Considering continuity and force equilibrium at the striker bar-input bar interface, one can obtain mathematical expressions to determine the strain in each bar.

$$\text{Continuity of motion: } v_0 + v_A = v_B \rightarrow C_{o,A}\varepsilon_A + C_{o,B}\varepsilon_B = -v_0 \quad (\text{Eq. 3.18})$$

$$\text{Force equilibrium: } F_A = F_B \rightarrow A_A E_A \varepsilon_A = A_B E_B \varepsilon_B \quad (\text{Eq. 3.19})$$

Hence, Solving the above simultaneous equations to obtain ε_A and ε_B :

$$\varepsilon_A = -\frac{A_B E_B}{A_B E_B C_{o,A} + A_A E_A C_{o,B}} v_0 \quad (\text{Eq. 3.20})$$

$$\varepsilon_B = -\frac{A_A E_A}{A_B E_B C_{o,A} + A_A E_A C_{o,B}} v_0 \quad (\text{Eq. 3.21})$$

If the striker bar and the input bar are made of the same material and of the same diameter (which is usually the case in typical SHPB tests), the amplitude of the strain in both bars will be the same and the value can be expressed as

$$\varepsilon_A = \varepsilon_B = -\frac{v_0}{2C_o} \quad (\text{Eq. 3.21})$$

Furthermore, the compressive wave (ε_A) that propagates leftward in the striker bar will eventually encounter free boundary at the left end and will be reflected as tensile wave. The generated tensile wave propagates rightward and upon reaching the impact face superimposes with the original input strain (ε_B). Since the reflected wave has the same magnitude as the incident wave, but is in opposite sign, the summation cancels each other out and dissipates the incident wave. Thus, the duration of the input pulse (T) can be estimated as the time required for the elastic wave to travel twice the length of the striker bar. **Fig. 3.10** summarises the sequence of incident wave propagation during the impact between the striker bar and the input bar.

$$T = \frac{2L}{C_o} \quad (\text{Eq. 3.22})$$

Fig. 3.10 The sequence of incident wave propagation during impact between the striker bar and the input bar

Hence, the incident wave in SHPB test is ideally a rectangular shape with the amplitude of $-\frac{v_o}{2c_o}$ and the duration of $T = \frac{2L}{c_o}$, as shown in **Fig. 3.11(a)**. However, in an actual test, the incident wave recorded by strain gauges installed at the mid-span of the input bar looked like **Fig. 3.11(b)**. The incident wave showed significant oscillations that fluctuated about the theoretical value and had gentler rise time than the ideal wave. The deviation of the actual pulse from the ideal pulse was caused by “dispersion” effect. This effect will be discussed in the subsequent section.

Fig. 3.11 Incident pulse (a) idealized (b) recorded in actual tests (Meyers, 1994)

3.5. Dispersion Effect

The propagation of elastic wave in long cylindrical pressure bar discussed so far is based on one-dimensional analysis, which is not necessarily true for real situations. In one-dimensional analysis, it is assumed that the particle displacement and the stress in longitudinal direction are uniform over the cross section of the bar and there is no stress in radial direction. In contrast with this assumption, the elastic wave propagation in finite diameter bar is generally three-dimensional; the particle displacement and the longitudinal stress vary over the cross section and there is stress in the radial direction.

The rigorous equations for the analysis of three-dimensional sinusoidal elastic wave propagation in isotropic cylindrical solids of infinite length were derived by Pochhammer (1876) and Chree (1889). The equations showed that the propagation velocity (i.e. the phase velocity) of a sinusoidal wave in slender cylindrical bars (C) was a function of two variables: Poisson's ratio (ν) and the ratio of bar diameter to wavelength of the harmonic wave (a/λ). In non-dimensional terms, the equation can be expressed as **Eq. 3.23**, where C_o is the wave propagation velocity in uni-axial direction.

$$\frac{C}{C_o} = f\left(\frac{a}{\lambda}, \nu\right) \quad (\text{Eq. 3.22})$$

Bancroft (1941) solved the Pochhammer-Chree equation for its fundamental vibrational mode and presented the results as plotted in **Fig. 3.12**. The exact mathematical solutions were actually very complex, but in essence, it demonstrated that short-wavelength (i.e. high-frequency) waves propagate slower than long-wavelength (i.e. low-frequency)

waves. Subsequent works by Davies (1948) also obtained similar finding. Davies (1948) solved Pochhammer-Chree equation for the first three vibrational modes assuming Poisson's ratio of 0.29, as shown in **Fig. 3.13**. It was clear that the propagation velocities also decreased as the wavelength increased for the first three modes. Nonetheless, further investigations on the wave propagation in typical SHPB tests showed that only the fundamental mode of vibration would normally be excited (Yew & Chen, 1978; Follansbee & Frantz, 1983).

Fig. 3.12 Pochhammer-Chree solutions for the fundamental mode of vibration in finite-diameter slender bar (Bancroft, 1941)

Although the propagating pulse in SHPB tests is generally not sinusoidal (but of rectangular shape as shown in **Section 3.4.4**), the pulse can be expressed as the sum of sinusoidal waves of different amplitudes and frequencies using Fourier Transform. Each frequency (or wavelength) component of the pulse propagates with different velocity due to the dispersive nature (i.e. the dependence of the phase velocity to the wavelength/frequency of the wave) of the propagating wave explained above. The shorter wavelength components travel more slowly than the longer ones, hence lagging behind the leading edge of the wave. Consequently, as the pulse travels along the bar, the sharp rise of the propagating pulse becomes gentler and the high-frequency oscillations trailing the main pulse become more evident. Essentially, the propagation of the pulse along the bar will change the shape of the pulse. This fact implies that the strain gauge reading taken at the mid span of the bar cannot be directly used to describe the incident pulse that impinges on the specimen. Thus, the recorded signal must be

corrected before being used to derive the stress-strain relationship of the test specimen. Alternatively, a pulse shaping technique, discussed in the next section, can be used to minimize dispersion effect.

Fig. 3.13 Pochhammer-Chree solutions for the first three vibrational modes for materials with Poisson's ratio of 0.29 (Davies, 1948)

3.6. Pulse-shaping technique

Since dispersion effect occurs due to variation in phase velocities with different frequency components of the pulse, theoretically it can be eliminated completely if the propagating wave consists of only one frequency component, i.e. a sinusoidal wave. Nonetheless, since in practice it is very difficult to produce a pure sine waveform during tests, the dispersion effect can only be minimized by generating a loading pulse with narrow frequency range. This can be done via one of the following ways: modifying the shape of the striker bar, inserting a tip material at the interface between the striker bar and the incident bar, or using a three-bar Hopkinson configuration.

Christensen *et al.* (1972) modified the geometry of the striker bar into a conical shape (in contrast to a cylindrical striker bar commonly used in SHPB tests). The modification led to an increase of the pulse rising time and a reduction of dispersive oscillations. Li *et al.* (2000) used a tapered striker bar to alter the shape of the incident waveform as shown

in **Fig. 3.14**. Their investigations showed that the modified striker bar when properly designed could produce an approximate half-sine waveform. The final stress-strain curves of the test specimens obtained by the new waveform showed much less oscillations and better represented the true material behaviour.

Fig. 3.14 A tapered striker bar used to minimize dispersion effect (a) the striker bar profile (b) the generated waveform (Li *et al.*, 2000)

Many other researchers (Frantz *et al.*, 1984; Follansbee, 1985; Gray III, 2000; Frew *et al.*, 2002) inserted a “tip material” between the striker bar and the input bar (**Fig. 3.15(a)**). The tip material—also known as a pulse shaper—was a thin disk (typically about 0.1-2mm thick). It could be made of copper, lead, brass, rubber, paper, plexi-glass, stainless steel, and aluminium. The choice of pulse shaper dimensions and materials depended on the desired strain rate and the behaviour of the material, which was usually unknown prior to the test. A credible and convincing method to guide researchers/engineers to select the pulse shaper dimensions and materials, unfortunately, is not yet available. Therefore, the selection remains a trial-and-error process. Nonetheless, the effectiveness of using a pulse shaper to minimize oscillations due to dispersion effect was confirmed (Frantz *et al.*, 1984; Follansbee, 1985). When a pulse shaper is hit by a striker bar, it deforms plastically and absorbs large amount of energy in the process. As a result, the compressive load transferred from the pulse shaper to the input bar escalates gradually, hence increasing the rise time of the pulse and dampening the dispersive high-frequency components of the excitation. **Fig. 3.15(b)** shows a slow-rising waveform generated due to the use of a pulse shaper.

(a)

(b)

Fig. 3.15 A tip material (a pulse shaper) used to minimize dispersion effect (a) a tip material sandwiched between the striker bar and the input bar (b) the generated waveform (Frew *et al.*, 2002)

Another alternative pulse-shaping technique is the use a three-bar Hopkinson configuration, whereby a dummy sample and an additional pressure bar are positioned between the striker bar and the input bar (**Fig. 3.16**). Ellwood *et al.* (1982) demonstrated that this method also effectively reduced dispersion effect. However, the need for an additional bar and a dummy specimen for each test discourages the use of this technique. Thus, the method is less popular than the previous two methods.

Fig. 3.16 A three-bar Hopkinson configuration (Ellwood *et al.*, 1982)

In the author's opinion, the pulse-shaping technique by inserting a tip material is preferred than that by modifying the striker bar because the former is more convenient

and more flexible than the latter; the trial-and-error process to find a suitable incident wave for different test materials can be done by just changing different dimensions and/or different materials of the pulse shapers (in contrast to fabricating a new striker bar with different geometries).

Besides dampening the oscillations due to dispersion effect, the application of pulse-shaping technique also increases the rise time of the incident wave. This condition ensures that dynamic stress equilibrium within the test specimens are achieved prior to specimen failure (Frantz *et al.*, 1984; Follansbee, 1985). Furthermore, a properly chosen pulse shaper can produce a nearly constant strain-rate in the specimen (Gray III, 2000). An equilibrium stress state and a constant strain-rate are necessary conditions for valid dynamic test results. However, it should also be noted that the use of pulse shaper may reduce the amplitude of the stress wave generated in the input bar since a significant amount of energy may be consumed by the pulse shaper as it deforms plastically. As a consequence, a higher striker bar velocity may be required to generate stress waves sufficient to fail the test specimens.

3.7. Wave propagation in a disk specimen

While the theory of elastic wave propagation in a long cylindrical bar is now matured and well-understood, the theory of stress wave propagation in a disk specimen of finite diameter and thickness is still difficult to understand due to complexity of the problem. Although the derivation of stress-strain relationship of the test material (as discussed in **Section 3.4.2**) was based on an assumption that the specimen deformed under uniform and uni-axial state of stress, many investigators (Kolsky, 1949; Davies & Hunter, 1963; Frantz *et al.*, 1984; Follansbee, 1985) actually observed that a transmission of the loading wave to the test specimen accelerated the specimen particles in both axial and radial directions. Furthermore, the presence of friction at bar-specimen interfaces may exacerbate the non-uniformity state of stress of the specimen (Davies & Hunter, 1963; Bertholf & Karnes, 1975). Several past authors had proposed correction methods to take into account these effects and had suggested ways to minimize these effects. Their contributions are summarized as follows.

3.7.1. Kolsky's correction method

Kolsky (1949) introduced a correction method based on energy balance approach. He described the discrepancy between the work done by the measured stress (i.e. in the

presence of inertial stress) and the work done by the actual stress (i.e. in the absence of the inertial stress) to produce a fractional strain due to the radial kinetic energy, as described in **Eq 3.23**.

$$A_s l_s \int_0^\varepsilon (\sigma_b - \sigma_s) d\varepsilon = KE_r \quad (\text{Eq. 3.23})$$

where A_s and l_s is the cross sectional area and the thickness of the specimen, respectively; σ_b is the average axial stress measured in the pressure bars; σ_s is the axial stress under ideal assumption (uniform and uniform stress condition); ε is the axial strain of the sample; and KE_r is the radial kinetic energy.

Assuming that the specimen was expanding uniformly and neglecting the frictional stress, Kolsky expressed the radial kinetic energy as

$$KE_r = \frac{1}{4} A_s \rho_s l_s v_r^2 \quad (\text{Eq. 3.24})$$

$$\text{where } v_r = v_s r_s \frac{d\varepsilon}{dt}$$

v_r is the radial velocity of a point on the specimen circumference; ρ_s , v_s , and r_s is the density, the Poisson's ratio, and the radius of the specimen, respectively; $\dot{\varepsilon}$ is the axial strain rate. Substituting **Eq. 3.24** to **Eq. 3.23** and differentiating, Kolsky obtained the correction equation (**Eq. 3.25**) for radial inertia.

$$\sigma_s = \sigma_b - \frac{1}{8} v_s^2 d_s^2 \rho_s \frac{d^2 \varepsilon}{dt^2} \quad (\text{Eq. 3.25})$$

Based on this equation, it could be predicted that the inertia effect would be minimized if smaller specimen diameter was used and strain rate was held constant.

In his derivation, Kolsky (1949) ignored the axial inertia effect since the specimen used in his experiment was very thin (i.e. aspect ratio $l_s/d_s \sim 0.05$) such that the uniformity of stress in the axial direction could be ascertained. Nonetheless, such specimen thinness unfortunately might induce considerable frictional stresses at the specimen-bar interfaces. To minimize the friction effect, Kolsky suggested applying thin lubricant layer at the interfaces.

3.7.2. Davies and Hunter's correction method

Davies and Hunter (1963) derived a more general correction method using similar concept to Kolsky's, but took into account the axial inertia effect. Their analysis resulted in **Eq. 3.26**.

$$\sigma_s = \sigma_b + \rho_s \left(\frac{1}{6} l_s^2 - \frac{1}{8} v_s^2 d_s^2 \right) \frac{d^2 \varepsilon}{dt^2} \quad (\text{Eq. 3.26})$$

It was noted that the last term of **Eq. 3.26** was identical to that of Kolsky's correction method for radial inertia. The equation also predicted that the inertia effect would be minimized if the strain rate was held constant. Moreover, the correction terms inside the parentheses would be completely eliminated if the specimen geometry satisfied the following relationship.

$$\frac{l_s}{d_s} = \frac{\sqrt{3}}{2} v_s \quad (\text{Eq. 3.27})$$

The result was confirmed by Bertholf & Karnes (1975) using finite element analysis. Thus, the use of specimen with geometry satisfying **Eq. 3.27** (i.e. specimen with "optimum geometry") was recommended wherever possible to simplify the analysis.

Davies and Hunter also addressed the issue of interfacial frictional effect presence in a thin specimen. Based on the frictional model by Siebel (1923) and the estimation of friction coefficient by Hawkyard & Freeman (1954), they concluded that the frictional effect could be neglected if the specimen satisfied the criterion below (**Eq. 3.28**). This criterion will be used as the design basis of test specimen dimensions as described further in **Section 3.7.3** and **Section 4.3**.

$$\frac{l_s}{d_s} \sim \frac{1}{2} \quad (\text{Eq. 3.28})$$

In addition, Davies and Hunter also emphasized that sufficient reverberations within the specimen were required to ensure dynamic stress equilibrium during deformation. They suggested that the rise time of the loading pulse (t) should be at least π times the specimen transit time—the duration required for a wave to travel the axial length of the specimen (l_s/C_s). For linear elastic solids, the longitudinal wave speed was defined

as $C_s = \sqrt{E_s/\rho_s}$. Hence, the minimum rise time for stress equilibrium (“ringing-up” period) was defined as

$$t^2 > \frac{\pi^2 l_s^2 \rho_s}{E_s} \quad (\text{Eq. 3.29})$$

For plastically-deforming solids complying Taylor-von Karman theory, the minimum rise time was expressed as

$$t^2 > \frac{\pi^2 l_s^2 \rho_s}{d\sigma/d\varepsilon} \quad (\text{Eq. 3.30})$$

where $d\sigma/d\varepsilon$ is the work-hardening rate of the stress-strain curve of the test specimen.

Following Davies & Hunter’s work, there were many other analysis methods proposed by different authors (Samanta, 1971; Bertholf & Karnes, 1975; Malinowski & Klepaczko, 1986). Despite using more advanced and detailed analysis technique, these authors found similar results to Davies and Hunter’s. Hence, it can be concluded that Davies and Hunter’s method are reasonable and very useful indeed.

3.7.3. Design of specimens for SHPB tests

It is clear from the above explanation that the dimensions of specimens not only play an important role in minimizing the radial and frictional effects, but also govern the condition of stress equilibrium. The length of the specimen should be sufficiently short so that a uniform state of stress can be achieved, but should not be too short such that the end-friction effect became dominant. The aspect ratio of the specimen should also be optimized to reduce both axial and radial inertia effects. Many authors (Frantz *et al.*, 1984; Gray III, 2000) claimed that a specimen with l_s/d_s ratio of 0.5-1 with application of lubricant at the bar-specimen interfaces would be ideal for SHPB tests.

Traditionally, the test specimen used for SHPB experiment is always a disk specimen with similar cross-section area to the pressure bars. Nonetheless, there is actually no restriction to use other specimen shapes, such as cubes or other square-sided shapes. The use of cube or prismatic specimens may actually offer several advantages over cylindrical samples. The prismatic specimens may not require surface treatment since the two loading surface of square-sided specimens will be automatically flat provided that the moulds used for casting the specimens are also flat, parallel and rigid. Also, the flat surface of prismatic specimens have an advantage over cylindrical samples for high-

speed imaging since the flat face of square-sided samples facilitates better surface mapping, hence allowing two-dimensional analysis using optical measurement technique. Nonetheless, limited information is available to assess the suitability of using prismatic specimens for SHPB tests.

3.8. General assumptions and conditions for valid SHPB tests

The derivation of stress-strain curves from SHPB tests was based on several assumptions and conditions that need to be satisfied to ensure valid test results. Subash & Ravichandran (2000) and Gama *et al.* (2004) presented a list of conditions for verification of SHPB experiment summarized as follows.

General assumptions and conditions related to the pressure bars

1. The stress-wave propagation in the pressure bars is one-dimensional. This assumption is valid under the following conditions:

a) The pressure bars are sufficiently long. Gray III (2000) suggested a length of the pressure bars to be at least 10 times the bar diameter for one-dimensional wave propagation.

b) The pressure bars remain isotropic, homogeneous, and elastic at all time during the test—the conditions that can be satisfied by a proper selection of bar materials. Also, it should be ensured that the amplitude of the excitation generated due to impact of the striker to the input bar should be below the elastic limit of the pressure bar.

c) The pressure bars are straight and are of uniform cross section over the entire length. Centreless grinding (i.e. a grinding process whereby a work piece is secured between two rotary grinding wheels) may be required to ensure these conditions. The bearings and the alignment fixtures to support the pressure bars should also be fabricated with high precision.

d) A dispersion-correction method (**Section 3.5**) or a pulse-shaping technique (**Section 3.6**) should be applied to take into account dispersion effect.

2. The stress distribution over the bar cross-section is uniform so that the measured strain by the strain gauges attached on the surface of the pressure bar represents the strain experienced by the whole cross section of the pressure bar. This assumption is automatically valid when the conditions at (1) are satisfied.

General assumptions and conditions related to the test specimen

1. The test specimen is under stress state equilibrium conditions during deformation. This can be achieved by limiting the thickness of the specimens.
2. The friction and inertia effects in the test specimens are minimal. These conditions are also attained by controlling the specimen dimensions as described in **Section 3.7**.
3. The bar-specimen interfaces should remain plane at all time.

3.9. Conclusion

In this chapter, a comprehensive review on split Hopkinson pressure bar tests is presented. Previous research studies related to SHPB test—testing methods, theoretical background, assumptions and conditions for valid SHPB tests—are reviewed.

CHAPTER 4: EXPERIMENTAL PROGRAMME

4.1. Introduction

The details of material mix design, specimen fabrication, calibration and execution of SHPB experiment are outlined in this chapter. Several limitations and possible ways to improve the present test setup are also discussed.

4.2. Material constituent and mix proportion

As a good starting point, the constituent materials and their proportions used in the present study (**Table 4.1**) were selected based on the information given by previous authors (Yang *et al.*, 2008; Chen *et al.*, 2013) with some adjustments to consider utilising raw materials that were obtained locally. As a control specimen, mortar specimens that had the same mix proportion as ECC with no fibre were also fabricated.

Table 4.1 The proportion of ECC mixture (by weight)

Binder (OPC + supplementary cementitious materials)	water	sand	High-range water reducer (HRWR)	PVA fibre (%volume)
1	0.27	0.2	0.008	2

The characteristic of the constituent materials of ECC is described as follows.

Ordinary Portland Cement (OPC)

The OPC used in this research was manufactured by Lafarge. It was a type I OPC for general concrete construction works conforming to SS EN 197 (SPRING Singapore, 2008). The raw materials used for cement production are easily obtained in nearly all regions of the world. Nonetheless, the production of Portland cement causes deleterious effects on environmental conditions, such as discharging harmful dust, consuming a large amount of energy (i.e. fuels), and emitting considerable greenhouse gas (i.e. CO₂). To alleviate these issues, partial replacement of cement with industrial by-products was considered in this study.

Supplementary cementitious materials from industrial by-products

Although the use of industrial by-products (such as fly ash/FA, ground granulated blast-furnace slag/GGBS, and silica fume/SF) as partial replacement of Portland cement is often justified by environmental and economic considerations, there are technical advantages brought about by the inclusion of these materials in ECC. They play a role as secondary cementing materials (i.e. react with water to form CSH, hence affecting the progress of hydration) and act as micro-fillers (i.e. fill the voids between fine cement grains). Thus, the addition of these materials generally results in the following outcomes:

- Increase composite density and strength
- Improve resistance against corrosion and alkali-aggregate reaction
- Decrease heat generation
- Improve workability

According to slag cement association (SCA, 2013), the chemical composition of GGBS resembles the chemical composition of Portland cement more than that of other minerals does. The composition of GGBS is also relatively more uniform than that of other minerals since GGBS is a co-product of a controlled process (i.e. iron production). For these reasons, a larger GGBS content, e.g. 50-80% replacement of Portland cement can be adopted. In the present study, 70% replacement of Portland cement by GGBS was adopted. The GGBS was obtained from EnGro Corporation Limited.

Water

Water is obviously an essential ingredient for hydration of cement compounds. Singapore's municipal water supply (i.e. tap water)—which is deemed suitable for drinking without further treatment—was used in this study.

Fine aggregates

In a typical composition of ECC, fine silica sand (with an average grain size and maximum grain size of 110 μm and 250 μm , respectively) is used as fine aggregates. Nonetheless, since silica sand was expensive and was difficult to obtain in Singapore, river sand (which was sieved to a maximum grain size of 600 μm) was utilized. It was anticipated that the change of sand type might affect the water demand of the composite

mix since river sand has higher water absorption capability than silica sand. Therefore, to achieve similar composite strength the sand content used in the present study was lowered. The sand/binder ratio in a typical ECC mix is 0.36, but the sand binder ratio in the present study is 0.2.

Superplasticizers (SP) / high range water reducers (HRWR)

HRWR is useful to improve workability of ECC mix which normally has low water/binder ratio. The addition of HRWR is essential in fibre-reinforced cement-based composites since the presence of fibres may adversely affect the fluidity of the fresh mix. In this study, HRWR type ADVA®181 from W. R. Grace (Singapore) Pte. Ltd. was used.

Fibres

Fibres are added in the mix to control cracks, to increase tensile strength, to improve ductility and toughness. The amount of fibres used directly affects the production cost of ECC. Hence, it is necessary to minimise the fibre content in the composition of ECC (generally 2% or less). There are various types of fibres available in the market for ECC production, e.g. steel fibres, carbon fibres, polyethylene (PE) fibres, polypropylene (PP) fibres, polyvinyl alcohol (PVA) fibres, etc. Nonetheless, ECC with PVA fibres has the largest experimental database and is the most commonly used to date. Also, on an equal volume basis, the cost of PVA fibres is less expensive than that of PE or steel fibres (Li *et al.*, 2001). Therefore, PVA fibres RECS15 produced by Kuraray Co., Ltd with properties shown in **Table 4.2** was adopted in the present study. To avoid premature rupture of PVA fibres due to their hydrophilic nature, the surface of PVA fibres is coated with 1.2% hydrophobic oiling agent as suggested by Li *et al.* (2001, 2002).

Table 4.2 Properties of PVA fibre (Kuraray Co Ltd, 2012)

Length (mm)	Diameter (mm)	Young's Modulus (GPa)	Tensile strength (MPa)	Elongation (%)	Specific gravity
8	0.04	40	1600	6	1.3

The final mass compositions of ECC and mortar per cubic metre of mix are shown in **Table 4.3**.

Table 4.3 The mix composition of ECC and mortar (kg/m³)

Mixture	OPC	GGBS	Water	Sand	HRWR	PVA fibre
ECC	429	1001	386	286	11	26
Mortar	438	1022	394	292	11	-

To characterize the tensile strain-hardening performance of ECC in comparison to that of mortar adopted in the present study, a uni-axial tensile test and a 4-point bending test were performed. The test results are presented in **Fig. 4.1** and **Fig 4.2** for the tensile test and the bending test, respectively. The results from uni-axial tensile test seemed to suggest that ECC adopted in the present study had a very low tensile strain capacity (less than 1%). On the contrary, the results from the 4-point bending test indicated a moderate tensile ductility of ECC (8-10 mm flexural deflection which corresponded to tensile strain capacity of 1.6 – 2%). The flexural deflection from 4-point bending test (δ) and the tensile strain in the extreme fibre of flexural specimen (ε_t) could be correlated by using principle of basic structural mechanics as discussed by Qian & Li (2007). The equation was expressed in **Eq. 4.1**.

$$\delta = \frac{15(\varepsilon_t/c)L^2}{162} + \frac{(\varepsilon_t/c)L^2}{4} \left(\frac{h}{L}\right)^2 \quad (\text{Eq. 4.1})$$

where h , L , and c are referred as follows:

h : the depth of 4-point bending specimen (= 12 mm)

L : the span of 4-point bending specimen (= 240 mm)

c : the neutral axis depth which can be taken as 0.9h (=10.8mm) for ECC specimens as recommended by Qian & Li (2007)

The discrepancy of tensile strain capacity obtained from direct tensile tests and 4-point bending tests was attributed to premature failure of the uni-axial tensile test specimens due to misalignment issue. This was evident from a significant difference of tensile displacement values recorded by two displacement transducers located at each side of the specimen. Thus, the tensile strain capacity obtained from 4-point bending test was deemed more reliable.

Fig. 4.1 Quasi-static uni-axial tensile test results

Fig. 4.2 Quasi-static 4-point bending test results

The tensile properties in terms of first cracking strength, ultimate strength, and strain capacity from three test samples are summarized in **Table 4.4**. Although the tensile ductility performance of ECC developed in this study is not as satisfactory as that reported by previous authors (Yang et al., 2008; Chen et al., 2013), it still shows significant ductility improvement compared to mortar.

Table 4.4 Tensile properties of ECC and mortar

Tensile properties	ECC	Mortar
First cracking strength (MPa)	3.20 ± 0.44	-
Ultimate tensile strength (MPa)	3.82 ± 0.36	2.96 ± 0.19
Tensile strain capacity (%)	1.76 ± 0.36	0.015 ± 0.003

4.3. Specimen dimensions

In the present study, two different specimen geometries were analyzed: specimens with square cross section (**Fig. 4.3(a)**) and specimens with circular cross section (**Fig. 4.3(b)**). The dimensions of specimens were designed so that the specimens occupied adequate cross sectional area, yet remained relatively small to minimize lateral inertia effect (**Section 3.7**). Also, it should be reminded that although larger specimens were preferred for better uniformity and representation of test results, they were difficult to handle in SHPB tests. Large specimens required a large diameter bar and a longer pressure bar (to ensure one-dimensional wave propagation). Eventually, the demand for space and the cost of test setup would also increase. Therefore, the specimens had to be designed for optimum dimensions.

To ensure sufficient homogeneity and adequate compaction during fabrication, it has been generally accepted that the minimum dimensions of test specimens should be at least 3 times the maximum size of the constituent particle of the composite (ASTM C192/C192M-14). Due to the absence of coarse aggregates, the largest constituent particle in ECC was assumed by the fibre component. Thus, if the fibre length of 8 mm was adopted, the specimen side length or specimen diameter of at least 24 mm would be adequate. Other important factors to consider in determining the specimen dimension were that the specimens must remain within the diameter of the pressure bar throughout the test and the areal mismatch between the specimen and the pressure bar should be minimal to avoid disturbance raised by geometric impedance difference.

Based on the above considerations, the side length of the square specimens and the diameter of the circular specimens were set to be 25 mm and 38 mm, respectively. Such dimensions fit well with the existing pressure bar of 40 mm diameter (**Fig. 4.4**). The specimen thickness of 23 mm was targeted for both geometries to minimize the inertia

effect and to ensure the stress equilibrium (recall from **Section 3.7.3**, the l_s/d_s ratio of 0.5-1 was recommended).

Fig. 4.3 Specimen dimensions (a) square cross section (b) circular cross section

Fig. 4.4 The specimens would remain within the cross sectional area of the pressure bar (a) square cross section (b) circular cross section

4.4. Specimen fabrication (mixing, casting, curing, and surface finishing)

To ensure the homogeneity and consistency of ECC specimens, the following mixing, casting, and curing processes were adopted:

1. OPC and GGBS were mixed in dry condition for a few minutes until it reached uniform condition. Since OPC and GGBS particles have distinct colour of medium grey and white, respectively, uniformity can be examined by visual inspection (i.e. when the mix reached homogeneous light grey colour).

2. Water and HRWR (pre-dissolved in the water) were added and were mixed until the fresh matrix achieved adequate fluidity and uniformity. It should be ensured that no clump formed during the mixing.

3. PVA fibres were incorporated slowly into the mix until all the fibres disperse evenly without balling. Also, the remaining HRWR was added in small increment until the desired workability was attained.

4. River sand was added and was mixed until uniform and consistent state was achieved. The mixing process took about 20-30 minutes.

5. The fresh mix was cast into moulds that had been coated by oiling agent for ease of de-moulding. The dimensions of the moulds were 25 x 25 x 25 mm (W x B x H) for specimens with square cross section and 38 x 150 mm (ϕ x H) for specimens with circular cross section (**Fig. 4.5**). Since the cylinder moulds were slender, the fresh mix was scooped into the moulds in five equal layers and was compacted between each layer by a vibrating table. On the contrary, only a single layer of fresh mix was scooped into the cube moulds and was compacted by a vibrating table to minimize voids and entrapped air.

Fig. 4.5 The moulds (a) cylinder (b) cube

6. The hardened cubes and cylinders were de-moulded after 24 hours and were air-cured in sealed conditions (using zip plastic bags) to prevent loss of moisture for at least 7 days.

Subsequently, the cubes and the cylinders had to be processed further to obtain specimens with desired sizes (as described in **Section 4.3**) and with flat, parallel end surfaces. For fabrication of specimens with circular cross section, the cylinders of 150 mm height were first cut into 5 disk specimens—each of which was 25 mm thick. Afterwards, the two end surfaces (top and bottom) were grinded to ensure flatness and levelness of the specimens. Due to some lost of thickness during the grinding process, the final thickness of the disk specimen was less than 23 mm. The cutting and the grinding operations were performed in Geotechnical Workshop using a diamond cutting machine (Buehler Delta™ Orbital and Chop Cutter) and a precision grinding machine (Chiao Bang CB-3060ASD), respectively.

Unlike the production of cylinder specimens, the production of cube specimens did not necessitate the cutting operation (since each individual cube could be cast and could be de-moulded easily using the mould depicted in **Fig. 4.5(b)**). Also, only a single surface (i.e. the top surface) required grinding operation. In fact, if the direction of casting (with respect to the direction of loading) was considered nonessential in governing the behaviour of the cube specimen, grinding would also be unnecessary because two pairs of flat, parallel surfaces existed naturally. Nonetheless, for fibre-reinforced cementitious composites such as ECC, the casting direction must be aforethought as it influenced the orientation of fibres in the specimen. As illustrated by **Fig. 4.6(a)**, during casting, the fibres tended to lie in the direction perpendicular to the pouring direction. It was hypothesized that if the casting direction and the compressive loading direction were parallel to each other, the fibres would be oriented in the most favourable direction to hold the opening of the crack (**Fig. 4.6(b)**). On the contrary, if the casting direction and the compressive loading direction were perpendicular to each other, the fibres would be oriented in the most unfavourable direction, thus would not be able to actively delay the crack growth (**Fig. 4.6(c)**). Therefore, to ensure a fair comparison between specimens with two different geometries (i.e. circular vs. square cross sections, both specimens were cast and were loaded in the same orientation. Thus, the square-sided specimens had to be grinded as well.

Fig. 4.6 The effect of casting direction on the crack growth of fibre-reinforced cementitious composites (a) casting direction affects the fibre orientation (b) casting direction parallel to loading direction (c) casting direction perpendicular to loading direction

Afterwards, the dimensions and the density of each specimen were measured to assess the overall consistency among all specimens. For more accurate measurements, the dimensions were measured using a Vernier caliper and the density was determined using Archimedes' principle. The average measured dimensions and densities from three specimens were summarized in **Table 4.5**.

Table 4.5 Average dimensions and densities of the specimens

specimens	Average side length / diameter (mm)	Average thickness (mm)	Average density (g/cm ³)
ECC square	25.18 ± 0.11	22.43 ± 0.94	2.05 ± 0.03
ECC circular	38.10 ± 0.05	21.35 ± 1.37	2.03 ± 0.04
mortar square	25.11 ± 0.15	23.22 ± 1.34	2.24 ± 0.10
mortar circular	38.05 ± 0.05	23.56 ± 0.90	2.24 ± 0.11

It was evident from **Table 4.4** that the measured densities were consistent, the side lengths/diameters were close to the target dimensions, but the measured thicknesses varied more, indicating average quality workmanship. In addition, it was interesting to

note that the density of ECC was lower than that of mortar—possibly owing to the increase of porosity due to fibre inclusion.

Lastly, prior to testing, the side surfaces of the specimens were lightly whitewashed with water-based paint so that the formation of crack patterns in the specimens could be easily identified. Since ECC was characterized by very fine cracks, it was recommended that only a single thin layer of paint was applied onto the surface of the specimens.

4.5. Description and characterization of SHPB system used in the present study

As discussed in **Section 3.3**, a typical SHPB system consisted primarily of two parts: impact component and signal processing component. In this section, detailed design and description of SHPB components in the present study is elaborated. An overall view of the SHPB system is shown in **Fig. 4.7**.

Fig. 4.7 Photo of SHPB system in Construction Technology Laboratory, NTU

4.5.1 Design of the gas gun

The gas gun was designed to launch a striker bar by releasing high-pressure nitrogen gas. It consisted of three chambers, a barrel, and a piston as described schematically in **Fig 4.8**. The following sequences were followed to launch the striker bar:

1. The striker bar was inserted into the barrel to a desired position.
2. The inlet valve in chamber 1 was opened letting nitrogen gas to flow into chamber 1 and moving the piston leftward.
3. The inlet valve in chamber 1 was closed and the inlet valve in chamber 2 was opened. The gas pressure level in chamber 2 was set to be identical to the gas pressure level in chamber 1. Since the effective area of chamber 1 was larger than that of chamber 2, the piston would not move rightward to its original position.
4. The inlet valve in chamber 2 was closed and the inlet valve in chamber 3 was opened. Nitrogen gas was charged into chamber 3 to a desired pressure level. It should be noted that the gas pressure level in chamber 3 would determine the impact velocity of the striker bar, hence controlling the generated strain-rates of the specimen.
5. The inlet valve in chamber 3 was closed. At these stages, all three chambers had been charged and the gas gun was ready for testing.
5. The gas in chamber 1 was suddenly released by rapidly opening the outlet valve. This action would cause the piston to move rightward due to the pressure in chamber 2, hence opening the entrance of the barrel. Consequently, the gas in chamber 3 would blow into the barrel, accelerating the striker bar.

Fig. 4.8 Gas gun system

It is clear from the above description that the striker velocity can be varied by adjusting the initial position of the striker bar in the barrel (i.e. the insertion depth into the barrel) or by modifying the gas pressure in chamber 3. In the present study, the later method was adopted. The insertion depth was kept constant at 1000 mm, but the gas pressure was varied between 2—6 bars. The relationship between the gas pressure and the resulting impact velocity is shown in **Fig 4.9**. It was clear from the test records that an increase of gas pressure increased the impact velocity, and increased the strain-rate imposed on the test materials. Nonetheless, some inconsistencies among recorded data at identical gas pressure level were also observed owing to variations in gas escape rate, valve release speed, or other factors that were difficult to control with the present setup. Moreover, it should be noted that the resultant impact velocity was also affected by the striker bar length; under identical gas pressure, the impact velocity generated by a long striker bar was smaller than that generated by a short one because the former was heavier than the latter.

Fig. 4.9 Varying the gas pressure in chamber 3 generated different impact velocities (data from preliminary test with striker bar length of 80 mm)

4.5.2 Signal measuring system

The strain signals were measured by a pair of strain gauges (TML FLA-3-11-3LT—3 mm gauge length and 3 m lead wire length) attached on opposites sides of the middle of the input and output bar surfaces. Each pair of strain gauges was connected through a bridge box (TML SB-120B) by “opposite half bridge 3-wire” configuration as show in **Fig. 4.10**. With this arrangement, it was expected that the influence of bending strain that might be generated due to an imperfect strain gauge alignment could be eliminated. The bridge boxes were then connected to a dynamic strainmeter (TML DC-97A) with a wide range of frequency response so that rapidly changing strain signals could be accurately recorded. Furthermore, the signals were sampled and were converted into digital form using a digital oscilloscope (Yokogawa ScopeCorder DL850). To accurately reconstruct the incoming signal, the oscilloscope sampled the signal at a constant time interval of $0.1 \mu\text{s}$ (i.e. a sampling rate of 10^7 data points per second). Finally, the recorded data was transferred to a personal computer for further processing. A summary of the signal measuring system is described in **Fig. 4.11**. It should also be noted that the signals were initially measured in the form of a voltage history. To convert it into a strain history, a conversion factor should be determined. With the setting values of strainmeter as shown in **Table 4.6**, an output voltage of 1 V corresponded to $5000\mu\epsilon$ (Tokyo Sokki Kenkyujo Co., Ltd, 2004).

Fig. 4.10 Strain gauges configuration

Table 4.6 Setting values of the dynamic strainmeter

Mode	Constant Voltage
Gauge factor	2.1
Bridge configuration	Opposite half bridge 3-wire
Low-pass filter	All pass
Constant voltage (CV) set value	2V
Rated output (RO)	1V
Sensitivity (SENS)	5250 $\mu\epsilon$

Fig. 4.11 Configuration of the signal measuring system

4.5.3. Impact velocity measuring system

The striker bar velocity upon impact was measured using a pair of OMRON photoelectric sensors and a time counter. The photoelectric sensor consisted of a transmitter that emitted infrared beam and a receiver that captured infrared beam. The arrangement of the velocity measuring system is illustrated in **Fig. 4.12**. When the striker bar passed through the first sensor, the infrared beam was blocked, signalling the time counter to

start recording the time. Subsequently, when the striker bar passed through the second sensor, the infrared beam was blocked, signalling the counter to stop recording the time. In other words, the blocked infrared beam in the first and the second sensors stimulated the time counter to start and to stop counting, respectively. By counting the time taken for the striker bar to travel from the first sensor to the second sensor and by knowing the distance between the two sensors (i.e. 30 mm), the impact velocity could then be calculated easily. For accurate impact velocity measurements, the sensors should be located just before the impact end of the input bar. Nonetheless, if the distance between the second sensor and the input bar was too close, the time counter might experience technical errors since the striker bar might bounce back, hence obstructing the second sensor again. In this study, the optimum distance between the second sensor and the end of the input bar was taken as 25 mm.

Fig. 4.12 Velocity measuring system

4.5.4 Design of the pressure bars and calibration of SHPB system

The material and geometrical properties of SHPB apparatus in Construction Technology Laboratory NTU had been assessed in the earlier studies by Zhao (2003) and Zhang (2004). They are summarized in **Table 4.7**. The pressure bars were designed such that the conditions for one-dimensional, elastic, homogeneous wave propagation along the bars as discussed in **Chapter 3** were met.

Table 4.7 Material and geometrical properties of SHPB Apparatus in Construction Technology Laboratory, NTU

Material properties		Geometrical properties	
Material	Steel	Bar diameter	40 mm
Young's modulus	205 GPa	Input bar length	1500 mm
Yield strength	1000 MPa	Output bar length	1000 mm
Density	7795 kg/m ³	Striker bar length	40, 80, 160, 220 mm
Poisson's ratio	0.27		

Based on the above data, the elastic wave propagation velocity in the pressure bar can be determined by applying 1D elastic wave propagation solution (**Eq. 3.4**), $C_0 = \sqrt{E/\rho} = \sqrt{\frac{(205 \times 10^9)}{7795}} = 5128.2$ m/s. For verification, the value was compared with the experimental value obtained from a single bar test (whereby the striker bar impacted the input bar and the output bar separately). By measuring the transit time of the recorded signals travelling back and forth along the pressure bar (**Fig. 4.13**), the elastic wave propagation velocity could be calculated as follows.

$$C_0 = \frac{\text{travel distance (= 2 x bar length)}}{\text{transit time}} = \frac{2 \times 1 \text{ m}}{0.0003884 \text{ s}} = 5149.3 \text{ m/s}$$

Folk *et al.* (1958) suggested using the corresponding time when the amplitude of the signal reached one-third of the maximum amplitude for more accurate transit time calculation.

Hence, it was shown that a very good agreement between the theoretical and the test values of C_0 was achieved. The difference between the two values was less than 0.45%. Furthermore, a comparison with the C_0 value reported by earlier studies (Zhao, 2003 and Zhang, 2004)—5131.4 m/s—also showed reasonable agreement.

Fig. 4.13 Recorded signal from the single bar test to verify the wave propagation velocity of the pressure bar (at striker impact speed of 5.57 m/s, striker bar length = 220 mm, without pulse shaper)

In addition, to further verify proper functioning of the SHPB system, the incident pulse waveform from the experimental test was compared to that from 1D elastic wave propagation theory. The experimental incident pulse waveform was derived by converting the signal from the recorded voltage history into strain history using the conversion factor described in **Section 4.5.2**. Focusing on the first interval of the recorded pulse, one obtained the experimental incident pulse waveform as shown by the solid line in **Fig. 4.14**. On the other hand, the theoretical incident pulse waveform was constructed using **Eq. 3.21** and **Eq. 3.22** as shown by the dotted line in **Fig. 4.14**. It can be seen that the waveform from the experimental test is superimposed by high frequency oscillations due to dispersion and inertia effects as discussed in **Chapter 3**. Nonetheless, the average peak strain (ε_i) and the pulse duration (T) obtained from the experiment are quite close to the theoretical predictions. Apart from the dispersion and inertia effects, other factors such as strain gauge misalignment error during installation, external disturbances (i.e. electrical noises from power supply cable or mechanical vibrations from nearby machines) might also contribute to the discrepancies between the experimental and the theoretical values.

Fig. 4.14 Comparison of incident pulse waveforms from measurement and 1D elastic wave theory (at impact speed of 5.57 m/s, striker bar length = 220 mm, without pulse shaper)

Moreover, to ensure proper transmission of strain pulse from the input bar to the output bar, the recorded signals obtained from a coupled bar test (whereby the striker bar impacted the input bar set in series with the output bar, without the specimen sandwiched in between) were also examined. From **Fig. 4.15**, it can be observed that the waveform of the incident pulse recorded by strain gauges in the input bar is very similar to the waveform of the transmitted pulse recorded by strain gauges in the output bar. Also, the peak value of the incident pulse is in a close agreement to the peak value of the transmitted pulse. These findings indicated satisfactory pulse transmission mechanism at the input bar-output bar interface. Nonetheless, a small reflected pulse—which theoretically was not supposed to be present in the coupled bar test—was observed in **Fig. 4.15**. This implied that there might be misalignment between the input bar and the output bar. Several attempts to resolve the misalignment issue had been made by adjusting the position of the support bearings, but perfect or nearly perfect alignment was very difficult to achieve in the real test. Therefore, the reflected pulse recorded in the actual test (in the presence of a specimen) must always be calibrated by subtracting it from the reflected pulse recorded in the coupled bar test under the same impact velocity (in the absence of a specimen).

Fig. 4.15 The recorded signals from the coupled bar test (without a specimen) to verify proper pulse transmission at the input bar-output bar interface

4.5.5 Design of pulse shaper

At higher impact speed (i.e. beyond 20 m/s), it was observed that the distortion due to dispersion and lateral expansion/contraction in the pressure bars was intensified. Hence, the pulse shaping technique as discussed in **Section 3.6** was necessary to alleviate this issue. A comparison of impact-generated incident waveform in the absence and in the presence of the pulse shaper is shown in **Fig. 4.16**.

Fig. 4.16 The comparison of incident waveforms with and without a pulse shaper (impact speed of 20 m/s, striker bar length = 220 mm)

In the present study, copper material (of 2 mm thick) was used as the tip material. Three different copper diameters (i.e. 10mm, 15 mm, and 22 mm) were examined in the preliminary tests to find suitable dimension effective to dampen the oscillations. From **Fig. 4.17**, it is evident that under identical test conditions reducing the copper diameter results in the following outcomes:

- 1) increases incident pulse rise time (favourable condition—since it provides more time for the specimen to achieve stress equilibrium prior to failure)
- 2) reduces oscillations due to dispersion effect (favourable condition—since it might be unnecessary to perform rigorous calculation for dispersion correction)
- 3) reduces incident pulse amplitude (unfavourable condition—since a higher gas pressure is required to generate the same amplitude. But it will not be an issue if the gas gun has sufficient capacity)
- 4) increases pulse duration (unfavourable condition—since longer pressure bars is needed to avoid overlapping between the incident and the reflected pulses. But it will not be a problem if the pressure bars have sufficient length)

Fig. 4.17 The comparison of incident pulse waveforms with three different copper diameters

A spectral analysis using Fast Fourier Transformation (FFT) algorithm was also performed to investigate the wave amplitude in frequency domain (as displayed in **Fig. 4.18**). The analysis confirmed that the incident pulse produced by smaller diameter pulse shaper was characterized by a narrower band of frequency components. This further implied that dispersion correction was unnecessary when a small diameter copper pulse

shaper was adopted. Based on these considerations, the 10 mm diameter copper was chosen as the ideal pulse shaper.

Fig. 4.18 Narrower band of frequency components with smaller diameter pulse shaper

4.5.6. The protective box

To protect personnel and other devices from flying debris and fragments of the crushed specimens, a special protective box as shown in **Fig. 4.19** was fabricated. The box was designed to be sturdy and robust, yet easy to assemble. It was primarily made of steel sheet, but the front face—which functioned as an observation window for taking pictures or videos—was made of clear acrylic plexiglass sheet. The left and right faces of the box were equipped with holes to allow the pressure bars to pass through them without any constraints. The top face of the box was easily attached and detached so that the debris/fragments contained in the box could be easily cleared after each test.

Fig. 4.19 Protective box

4.6. High speed camera

As a means to identify and to evaluate the prevailing mode of failures during high-strain rate compression tests, a high-speed photography technique was adopted. A high-speed, PC-based digital camera (Photron Fastcam 1024PCI or Phantom V310—used interchangeably depending on its availability) was employed for this purpose. The specifications of the high-speed cameras are summarized in **Table 4.8**.

Table 4.8 Specification of high-speed digital camera used in the present study

Camera type	Photron Fastcam 1024PCI	Phantom V310
max resolution (Horizontal x Vertical)	1024 x 1024	1280 x 800
pixel size (μm)	17	20
frame rate at max resolution (fps)	1,000	3,250
max frame rate at min resolution (fps)	109,500	500,000
frame rate at resolution of 256 x 256 (fps)	10,000	40,500
max shutter speed (μs)	1.5	1
sensor	colour	monochrome

The image resolution is a measurement of the width and the height of an image in terms of total number of pixels; the greater the number of pixels (i.e. associated with higher resolutions), the better the quality of the images. On the other hand, the frame rate is a measurement of the number of frames that can be captured consecutively by the camera

in one second; the higher the frame rate, the greater number of images would be generated for a given recording time. Thus, to obtain adequate number of frames with great quality images, both the resolution and the frame rate should be set to the maximum capabilities of the camera. Unfortunately, in most photographic devices including high speed cameras, there is always a trade-off between the resolution and the frame rate; higher resolution results in lower frame rate, and vice versa. Therefore, to get the right balance between the quality and the quantity of images, the setting of the camera system should be carefully configured.

It was identified from several trial tests that the minimum resolutions of 256 x 256 must be achieved if clear, sharp images to describe crack or damage patterns in the specimen were expected. With this resolution setting, Photron Fastcam 1024PCI and Phantom V310 can capture 10,000 and 40,500 frames per second (fps), respectively. In general, Phantom V310 has better performance than Photron Fastcam 1024PCI; for a given recording time and a specific resolution setting, the former captures more images than the latter. Unfortunately, due to increasing demand for using Phantom V310 from many laboratory users, there was limited opportunity to use the camera. Thus, Photron Fastcam 1024PCI was utilized more frequently than Phantom V310.

Besides the frame rate and image resolution, another critical factor for successful imaging of dynamic failure of the test materials is the light intensity. Due to limited exposure time during high-speed image recording, the intensity of the light source should be magnified. A pair of dedolight lighting unit (Dedocool Light Head) specially devised for high-speed photography was used in the present study. The lighting unit was designed to utilize a low wattage, low voltage lamp arranged in combination with a unique optical system and special reflector such that it was able to generate a high intensity of light. It should be noted that the lighting unit might heat up after a prolonged shooting session. Thus, to prevent electrical damages and fire due to overheating, the lighting unit must not be left switched on continuously for more than three hours.

In addition, to ensure that the camera recorded the complete sequential failure process of the specimens, the camera should be triggered at the right time—prior to the time when the incident pulse reached the specimen. Three different triggering techniques were considered:

1) Manual trigger

In manual trigger, the user controlled the triggering event manually; the triggering button was usually pressed at the same time as the striker bar was released. This technique not only required an additional manpower to control the triggering of the camera, but also relied on human reaction time (i.e. typically about 0.2 s). Since human reaction time was relatively slow compared to the time required for the incident pulse to reach the specimen, most of the time the camera was triggered too late to capture the first crack.

2) Auto trigger using strain gage signals

In this technique, the camera was automatically triggered when the strain gauge detected the presence of incident pulse. This technique was generally preferred for high-speed tests since the captured images could be easily synchronized with the strain gauge signals (as both of them were recorded at the same starting time). Nonetheless, the automatic triggering method that relied on strain gauge signal was not always guaranteed since strain gauge output voltage might be lower than the minimum voltage required to trigger the camera.

3) Auto trigger using photo electric sensor signals

Another alternative to trigger the camera automatically was by using the signals from the photoelectric sensor to measure the striker bar velocity. When the striker bar passed the sensor and blocks the infrared beam, a signal was re-routed to the camera system, and triggered the camera. This triggering technique was adopted in the present study. With this technique, it was expected that the recording contained sequential images taken before, during, and after the cracking process.

4.7. Procedure of SHPB Tests

The complete sequence of operation of the SHPB experiment was as follows.

1. All the electronic equipment, such as the dynamic strainmeter, the digital oscilloscope, the time counter, the computer, and the high-speed camera were switched on (the lighting unit in this stage was excluded since it easily heated up; the lighting unit would be turned on just prior to the release of the striker bar).
2. The alignment of the pressure bars was inspected and the support bearings were adjusted if necessary. Oil and/or grease lubricant was applied on the bearing

surface to ensure that the pressure bars could move freely along their longitudinal axis with minimum friction.

3. The striker bar was inserted into the barrel to a desired depth and the pulse shaper was glued on the impact surface of the input bar by applying grease.
4. An adequate mount of grease was smeared on the specimen surfaces to minimize specimen-bar contact frictions. The amount of grease applied should be neither too little (such that significant boundary frictions affected the specimen behaviour) nor too much (such that the specimen became easily slide and fall).
5. The specimen was inserted between the input bar and the output bar. The input bar, the specimen and the output bar were aligned carefully for proper wave transmission.
6. The gas gun was charged with nitrogen gas using the procedure described in **Section 4.5.1**. Usually, it took about 5 minutes to charge the gas.
7. The setting of the strainmeter, the oscilloscope, and the time counter was checked. The devices were put on standby mode to record data.
8. The lighting unit was turned on and the setting of the high-speed camera was configured. The positions of the camera and the lighting were adjusted until a clear, sharp image appeared in the monitor.
9. When the gas gun was fully charged and all the other devices were in standby mode, the testing could be started. At this stage, all personnel were required to wear personal protective equipment (PPE) for hearing protection (e.g. ear plug or ear muff)—since the impact between the striker bar and the input bar would generate a very loud sound.
10. The start recording button of the camera was pressed to let the camera ready to capture images. The valve of the gas gun was opened to propel striker bar.
11. After the test, the travelling time taken by the striker bar to hit the incident bar was recorded to determine the impact velocity. Also, the strain history data from the oscilloscope and the captured images from the high-speed camera were saved.
12. Fragments and debris of the crushed specimen in the protective box were cleared and the contact surfaces of the pressure bars were cleaned with a dry cloth.
13. Steps 3-12 described above were repeated for the subsequent tests.

14. After finish the tests, it must be ensured that the valves in the cylinder tank (where the nitrogen gas was stored) was closed properly and all the electronic devices were turned off.

CHAPTER 5: RESULTS AND DISCUSSIONS

5.1. Introduction

There were two series of tests conducted in this study. The first one was performed to accomplish the primary objective of the study, i.e. to study the effect of strain rates on the uni-axial compressive behaviour of ECC, while the second series was performed to accomplish the secondary objective, i.e. to study the effect of specimen geometries—square vs. circular cross section—on the behaviour of cementitious specimens in SHPB compressive tests. Nonetheless, both series would be useful to study the difference between ECC and mortar performances under dynamic compression and to provide valuable input for material modelling in numerical simulations.

5.2. The effect of strain-rates on the stress-strain relationships of ECC and mortar under uni-axial dynamic compression

Fig. 5.1 shows the comparison of stress-strain curves of ECC and mortar under 5 different loading rates: quasi-static condition (bold solid lines) and dynamic impact conditions at striker velocities of 25m/s (dotted lines), 30 m/s (dashed lines), 35 m/s (dash-dot lines) and 40 m/s (solid lines). The stress-strain curves were generated by adopting data processing technique as described in **Sections 3.4.2** and **3.4.3**. By using the recorded strain signals from the tests and applying **Eq. 3.13**, it was recognized the striker impact velocities of 25 m/s, 30m/s, 35 m/s, and 40 m/s would deform the specimens at strain rates about 250/s, 300/s, 450/s, and 600/s, respectively.

The general outlook of the stress-strain curves of ECC and mortar at any strain rates can be described as follows: an initial linear ramp up followed by a non-linear ascending branch up to the peak stress and finally a non-linear descending branch. It was evident from the curves in **Fig. 5.1** that the behaviour of ECC and mortar was sensitive to strain rates. As the strain rate increased, the pre-peak ascending branch became steeper, the peak compressive stress increased, and the strain at peak stress increased.

Thus, the compressive stress-strain curves obtained from the SHPB tests generally display much better mechanical properties—i.e. higher strength and better ductility—than those obtained from the quasi-static tests. The observed enhancement of the

mechanical properties might be contributed by two sources: inherent rate-dependent behaviour of the material (i.e. unique features of dynamic fracturing of the material) and/or external influences from the testing conditions (i.e. boundary confinement, radial inertia, specimen size, etc.). Both sources are discussed in the following sections.

(a)

(b)

Fig. 5.1 The stress-strain relationships of (a) ECC and (b) mortar under uni-axial dynamic compression at various strain rates

I. Unique features of dynamic fracture

In dynamic fracture, the presence of stress waves arisen from an external applied load and/or a released stress at the crack tip complicate the propagation of a crack; the stress waves reflected at the specimen free boundaries may return to the crack tip and change the crack tip stress-state condition. Under the same initial flaw size and the same driving force, past researchers (Freund, 1990; Meyers, 1994) through rigorous derivation of analytical solution or through thorough observation during experimental tests showed that the stress intensity around the tip of propagating crack was generally smaller than that under stationary crack. This finding implied that it was more difficult to initiate unstable crack growth under high-strain rate conditions. Consequently, the crack required higher applied stress to initiate failure at higher strain rates, thus increasing the peak compressive strength as the strain rate increased.

Fig. 5.2 The final damage of the specimens at varying strain rates (a) ECC and (b) mortar

Another characteristic of dynamic fracture that is different from the quasi-static fracture is the crack propagation pattern. From the comparison of the final damage of ECC specimens at varying strain rates (as shown in **Fig. 5.2(a)**), it is apparent that as the

strain rate increases, the cracks tend to bifurcate into many branches leading to fragmentation. In the case of mortar specimens (as shown in **Fig. 5.2(b)**), it is also evident that the fragment size decreases as the strain rate increases. The phenomenon is similar to shattering of a glass when subjected to impact (while only a single crack formed when subjected to quasi-static loading). The crack branching occurs in order for the system to quickly release the kinetic energy through the formation of two or more new surface cracks. The dissipation of energy in this manner might explain why the critical strain and the overall energy absorption capacity of the material at dynamic conditions are higher than that at quasi-static condition.

II. Boundary restraint and aspect ratio of the specimen (L_s/d_s)

It has been widely recognized that the measurement of compressive strength of cementitious composites are highly influenced by the boundary condition and the aspect ratio of the test specimen (Neville, 1996). When a test specimen is uni-axially compressed, it tends to expand in the direction perpendicular to loading direction due to Poisson's effect. If the expansion is restrained by the boundary friction, transverse stresses which may act as confining stresses will be built up in the specimen. The confining stresses inhibit crack growth and cause the specimen to fail at higher applied load. For a slender specimen (i.e. aspect ratio above 2), the confined zone only extends to a certain distance near the specimen end, leaving the central portion free to expand. Thus, the boundary restraint effect in this case might not affect the strength measurement. On the other hand, for a compact specimen (i.e. aspect ratio below 2), the confined zone extends to nearly the whole volume of the specimen. In SHPB tests, however, the use of compact specimens cannot be avoided. In fact, it has been widely practiced to use specimens with an aspect ratio in the range of 0.5 to 1 to ensure dynamic stress-state equilibrium condition within the specimen over the short loading duration (**Section 3.7.3**). To minimize boundary friction, the end faces of the specimen were grinded and were covered by a thin layer of lubricant.

To confirm the effectiveness of the lubricant in reducing boundary restraint effect, quasi-static compression tests—with and without lubrication—on square-sided mortar specimens were conducted. As shown in **Table 5.1** and **Fig. 5.3**, the presence of a thin layer of lubricant lowers the measured compressive strength and changes the damage pattern of the specimen—from a cone-shaped failure pattern (which indicates that

damage mostly occurred in the outer layer of the specimen) to a columnar splitting tensile failure pattern (which displays more uniform damages across the specimen cross-section). For SHPB test, it was further speculated that the reduction of boundary friction due to lubrication effect would be even more significant—since once the relative motion between the specimen and the bar was allowed, the coefficient of friction was reduced to that of kinetic friction (which was typically smaller than that of static friction). Moreover, in SHPB test setup, the specimen was secured in between the pressure bars in horizontal direction, thus sliding between the specimen and the pressure bars became relatively easier compared to that in standard quasi-static compression tests whereby the specimen was held in vertical direction. Based on these reasons, a minimum boundary friction condition in the SHPB test can be justified.

Table 5.1 The comparison of measured quasi-static peak compressive strength (in MPa) between specimens tested with and without application of interface lubricant

Test	without lubricant	With lubricant
1	87.04	70.88
2	82.72	82.24
3	99.36	81.12
average	89.71	78.08
standard deviation	8.63	6.26

Nevertheless, after the specimens have reached their peak capacity and have undergone significant deformation, the lubricant layer at bar-specimen interfaces might be squeezed out or might be absorbed into the pores or the cracks of the cementitious matrix, thus increasing the frictional stresses at the contact regions. If this happens, the confining pressure induced by the frictional stresses will give rise to an apparent enhancement of the ductility of test materials (van Vliet & van Mier, 1996; RILEM TC148-SSC, 2000).

(a)

(b)

Fig. 5.3 The damage pattern of square-sided mortar specimen under quasi-static compression load (a) without a thin layer of lubricant on the bar-loading platen interface (b) with a thin layer of lubricant on the bar-loading platen interface

III. Radial inertia effect

Under dynamic event, another important factor that may affect the stress-strain measurement is inertia effect. When a specimen is subjected to impact, a plane compressive wave propagates into the specimen and induces the specimen to contract in the axial direction as well as to expand in radial direction as a result of Poisson's effect. However, under rapid loading conditions, the specimen does not have sufficient time to instantaneously and uniformly expand in radial direction, thus imposing confining stresses on the core of the specimen. Janach (1976) and Bischoff & Perry (1991) described this phenomenon as a transition from uniaxial strain state condition to uniaxial stress state condition.

Despite its importance, the influence of radial inertia confinement effect was difficult to analyze from the experimental test data. Hence, alternative approaches by analytical derivatives or numerical models should be adopted. As discussed in **Section 3.7**, Davies & Hunter (1963) derived an analytical solution to quantify the axial and radial inertia effects in a cylinder specimen when subjected to dynamic impact. By applying their analytical solution (**Eq. 3.26**), the stress-strain relationship obtained from dynamic measurement could be corrected as shown in **Fig. 5.4**. The comparison of the stress-strain relationships with and without inertia correction in this figure seemed to suggest that the inertia effect was so small that it could be negligible in the measurement. Unfortunately, the derivation of analytical model for inertia correction of a square-sided specimen was not yet readily available due to its more complex geometrical configuration. An attempt to describe the inertia effect by means of numerical simulation had been made despite insufficient studies about the material model for ECC and mortar. This issue is elaborated further in **Appendix** where the difference of inertia effect between the square-sided and the circular-sided specimens are discussed.

Fig. 5.4 The comparison of stress-strain relationship of ECC cylinder specimen with and without inertia correction method

5.3. The comparison of stress-strain relationships of ECC and mortar under different strain rates

In this section, the comparison of stress-strain curves of ECC and mortar under several different strain rates are evaluated and the competitive advantages of adopting ECC material over mortar (as control material representing cementitious composites that has comparable chemical composition and similar static compressive strength with ECC) are discussed. The comparison of dynamic compressive behaviour between ECC and

mortar is shown in **Fig 5.5** (from test of square-sided specimens) and **Fig 5.6** (from test of circular-sided specimens). It is evident from these figures that the initial ascending portion of the stress-strain curves of ECC and mortar is similar, but starts to deviate from each other near the peak region. ECC curves generally maintain the peak values at greater range of strains, implying more stable crack propagation. Furthermore, neither ECC nor mortar fails instantaneously after reaching the peak strength. Both materials show gradual descending branch, commonly termed as strain-softening behaviour. Nonetheless, the post-peak branch of ECC specimens displays more gradual decrease than that of mortar specimens, indicating greater energy absorption capacity of ECC materials. It is hypothesized that the fibre bridging action similar to that observed in quasi-static uni-axial tension test contributes to this behaviour. This finding is consistent with the observed crack patterns that show the formation of fine cracks parallel to loading direction. This will be discussed in **Section 5.5**.

Fig. 5.5 Comparison of stress-strain relationships of ECC and mortar from SHPB tests of specimens with square cross section at several different strain rates (a) 250/s (b) 350/s (c) 450/s (d) 600/s

Fig. 5.6 Comparison of stress-strain relationships of ECC and mortar from SHPB tests of specimens with circular cross section at strain rates about 600/s

Moreover, it is also evident from the comparison of **Fig. 5.5** and **Fig 5.6** that the discrepancies between ECC and mortar specimens are more readily apparent when circular-sided specimens are utilized. This finding rationally implies that the measurement of stress-strain relationship in the post-peak range was significantly affected by the geometrical shape of the specimen. Despite possible influence from boundary restraint at large deformation, it is sensible to question which specimen shape actually gives more reasonable results closer to the real material behaviour. To find the answer of this question, several factors need to be further investigated as described in the following section.

5.4. The effect of specimen geometries on the behaviour of cementitious specimens in SHPB compressive tests

Fig. 5.7(a) and **(b)** show a comparison of the stress-strain relationships between the square- and circular-sided specimens in the case of mortar and ECC, respectively. Three specimens were tested in each case to ensure repeatability and consistency of the results. With the average impact speed of 40 m/s, the average strain rates attained in this series of test (derived using **Eq. 3.13**) were in the range of 476 to 658/s. The variations might be attributed to the discrepancies in gas escape rate or valve release speed of the gas gun system, material heterogeneity, or dimensional tolerances of the specimen. Nonetheless, since the resulting strain rates from each test were still of the same order of magnitude, the test data was comparable. In general, the test results were clear and repeatable. But, it was noted that the experiment with ECC specimens

produced more consistent results than that with mortar specimens probably due to ECC greater capability to control early age thermal or shrinkage cracks after casting.

From mortar test results (**Fig.5.7 (a)**), it is shown that the overall stress-strain profiles obtained from cuboidal specimens (dotted lines) and cylindrical specimens (solid lines) diverge from each other at large strain region. Although the initial ascending branches were alike, the cuboidal specimens showed lower peak compressive strength and gentler descending branches than the cylindrical specimens. On the other hand, from ECC test results (**Fig. 5.7 (b)**), the differences of stress-strain curves between the two specimen geometries are less noticeable probably due to seemingly activation of fibre bridging action. Regardless of the specimen shape, ECC always displayed much progressive declining curves.

(a) mortar

(b) ECC

Fig. 5.7 Comparison of stress-strain relationships between square-sided and circular-sided specimens (a) mortar (b) ECC

In the present tests, the two specimen geometries under investigation had the same thickness, but different aspect ratios (0.92 for cuboidal specimens vs. 0.61 for cylindrical specimens). Nonetheless, the experimental results shows that the stress-strain relationship obtained from the two geometries are close to each other up to the peak capacity. The similarity of the ascending branch may be due to minimum friction at the specimen-bar interfaces. Under frictionless boundary conditions, the stress-strain measurements become independent from specimen aspect ratios (van Vliet & van Mier, 1996)—and perhaps also becomes insensitive to different specimen geometrical shape.

Nevertheless, once the specimens had undergone significant deformation (i.e. large strains), the lubricant layer might no longer be effective to minimize friction as it could have been squeezed out or absorbed into the pores or the cracks of the cementitious matrix. Consequently, at large strain regime, the stress-strain measurement might not reflect the true material behaviour as it was highly affected by the boundary friction. In the case of brittle material like mortar, it is clearly shown that the square-sided specimens were affected more severely by the change in boundary condition than the circular-sided specimens. The presence of sharp edges in the square-sided specimens might have induced stronger confining stress to the central core of the specimens, hence caused

them to seem more ductile. Further investigations on the specimen modes of failure also revealed similar conclusion.

5.5. Failure modes

Identifying different modes of failure is important as it can explain the variations in the experimental data and hence useful to sort out the valid stress-strain curves. With the aid of the high-speed camera, it was observed that most specimens in this study failed by splitting tensile failure mode. Nonetheless, in the case of cuboidal specimens, this failure mode often occurs in a combination with other modes (e.g. corner or shear failures).

It should be noted that the failure modes observed in the present study were classified based on the crack patterns seen from only one side of the specimen (since only one high-speed camera was employed in each test). The appearance of crack from different perspectives might actually show different failure patterns. Also, it is recognized that the two-dimensional outward appearance of cracks on the surface of the specimen might not accurately reveal the failure mechanism of the whole body of the specimen because cracks actually propagate in three-dimensional space and their patterns are affected by the presence of flaws or pores near the surface. Despite these limitations, the surface crack patterns are still considered as useful indicators to identify failure mechanisms of the specimen.

1. Splitting tensile failure mode in cylindrical specimens

This mode of failure is characterized by the formation of cracks parallel to the loading axis as shown in **Fig. 5.8(a)** and **Fig. 5.8(b)** for ECC and mortar cylindrical specimens, respectively. The observed direction of cracks suggests that the specimens tend to expand in lateral direction due to Poisson's effect and eventually fail due to its limited tensile strain capacity. Since ECC possess superior attributes in terms of tensile strain capacity, it is expected that ECC specimens that failed by this mode of failure gives better performances than mortar counterparts. In fact, the photographs taken by the high-speed camera (in **Fig. 5.8**) and the stress-strain profiles (in **Fig. 5.9**) support this notion. As shown from the photographs, the ECC specimen displays a formation of multiple micro-cracks, while the mortar counterpart shows a formation of a single macro-crack. Also, at compressive strain rates beyond 500/s the ECC specimen disintegrates into few large

fragments, but the mortar specimen bursts into many small fragments (**Fig. 5.10**). The stress-strain curves show that the declining branches of ECC specimens are more gradual than that of mortar specimens, implying successful mobilization of fibre-bridging actions. The emergence of short plateau in stress-strain profile of ECC (**Fig. 5.9**) further confirms ECC superior ductility and high-energy absorption capacity even at high strain-rates.

Fig. 5.8 Typical crack patterns of cylindrical specimens failed by splitting tensile mode
(a) ECC (b) mortar

Fig. 5.9 Stress-strain curves obtained from cylindrical specimens failed by splitting-tensile

Fig. 5.10 Final damage of cylindrical specimens after the tests (a) ECC (b) mortar

It was also observed from **Fig. 5.9** that ECC specimens exhibit lower peak compression strengths compared to mortar specimens. This might be attributed to the inclusion of fibre in ECC specimens. The presence of fibre introduced higher porosity or bigger initial flaw size in the composite causing ECC specimens to fail at lower stress.

The splitting tensile failure mode was also observed in cuboidal specimens although it mostly appeared in combination with other failure modes, i.e. localized corner and diagonal failure.

2. Combined splitting tensile, corner and diagonal failure mode in cuboidal specimens

The failure mode is characterized by the formation of splitting tensile cracks and localized cracks near the corner region as shown in **Fig. 5.11**. Sometimes additional diagonal cracks also appear and merge with the splitting cracks as shown in **Fig 5.12**. The localized corner cracks occur due to the concentration of stresses building up near the sharp edge and the diagonal cracks may exist due to complex stress-state conditions in the specimen body—that eventually force the crack to propagate in diagonal direction. It is interesting to note that 3D numerical investigation (as shown in the **Appendix A**) reveals that when a cuboidal specimen is subjected to dynamic axial loading, the axial stress wave initially propagates along the specimen thickness with a concave-shaped wave front, while that in a cylindrical specimen propagates with a planar-shaped wave front. The complex concave-shaped wave front in a cuboidal specimen likely occurs due to areal mismatch between the cross section of the pressure bars and the specimen that eventually generates complex multi-mode vibrations at the interface. This condition together with lateral and axial inertia effects complicates the stress distribution over the

specimen body. **Fig. 5.13** shows the comparison of initial distribution of axial stress and its propagation direction between circular- and square-sided specimens when the stress wave propagates along the specimen thickness. Furthermore, the imminent increase of frictional boundary restraint at large deformation further exacerbates the complexity of stress-state condition in the specimen body.

In this failure mode, it is also observed that the ECC specimen displays smaller crack-width and less catastrophic damage than the mortar counterpart. Nonetheless, the comparison of stress-strain curves (**Fig 5.14**) between ECC and mortar specimens that failed by this manner looked similar—the post-peak curves of ECC (solid lines) displays only slightly gentler slope than that of mortar (dashed lines).

Fig. 5.11 Crack patterns of cuboidal specimens failed by a combined splitting tensile and corner failure mode (a) ECC (b) mortar

Fig 5.12 Crack patterns of cuboidal specimens failed by a combined splitting tensile and diagonal failure in ECC

Fig. 5.13 The distribution of axial stress and its propagation direction when the stress wave first reaches the specimens (a) cylindrical specimen (b) cuboidal specimen

Fig 5.14 Stress-strain curves obtained from cuboidal specimens failed by shear cone/hourglass mode

In conclusion, both cuboidal and cylindrical specimens will actually produce comparable dynamic stress-strain curves as long as the boundary friction is kept to a minimum degree. Although the cuboidal specimen tends to develop non-uniform stress distribution due to its sharp corner, the average stress of the whole specimen cross section still gives comparable value to that given by the circular-sided specimen. Nonetheless, at large deformation—when the lubricant layer has been squeezed out and the boundary restraint effect becomes dominant—the stress distribution in cuboidal specimen is actually closer to a tri-axial stress-state condition. This condition results in an apparent

ductility of the stress-strain curve obtained from cuboidal specimens, which might be further misinterpreted as the inherent ability of test materials to absorb energy. Thus, the test results at large strain (i.e. the post-peak branches of the stress-strain curves) from the cuboidal specimens should be taken with caution.

Besides failed by the above modes, specimens could also prematurely fail due to specimen falling/sliding/rotating prior to incident stress arrival. These conditions usually happened during the release of the compressed gas to propel the striker bar. Since the gas gun system and the pressure bars rested on the same supporting beam, the release of the compressed gas might introduce a small vibration that disturbed the stability of the specimen. It was also suspected that an excessive application of lubricant on the specimen-bar interface and a poor specimen surface preparation might also cause this premature fall. The falling usually happened during a short duration just before the striker hit the incident bar, thus it was hardly perceived by the author during the experiment. To identify the fall, the footages from a high speed camera as shown in **Fig. 5.15** must be examined. The stress-strain curves of the specimens that experienced such premature failure were not reported in this thesis as they were not valid test results. Nonetheless, the footages from the high speed camera were included in this section to emphasize the importance of having flat, parallel contact surfaces and applying a right amount of lubricant on the bar-specimen interfaces to avoid wasting test samples. The lubricant should be neither too little (such that significant boundary frictions affected the specimen behaviour) nor too much (such that the specimen became easily fall, slide, or rotate).

Fig. 5.15 Specimen fell down prior to incident stress wave arrival

5.6. Dynamic Increase Factor (DIF) of ECC and its comparison with DIF formula in the fib Model Code for Concrete Structure (MC2010)

The comparisons of DIF obtained from the present study and that from MC2010 (fib, 2013) are divided into two sub-sections: DIF for compressive strength and DIF for critical

strain. The DIF for modulus of elasticity is not included since the measurement of modulus of elasticity from SHPB tests is not reliable—due to difficulty to achieve stress equilibrium condition at the beginning of the tests (Gray III, 2000). It should be noted that fib (2013) actually recommends that DIF equations in MC2010 to be used only for strain rate up to 300/s. Nonetheless, in the present study it is assumed that these equations remain valid beyond the recommended strain rate limit.

The test results of ECC and mortar under dynamic compression from Chen *et al.* (2013) and Rong *et al.* (2010), respectively are also included in this section for comparison purposes. The experimental results by these authors are chosen since they adopted similar composite mix design with the ECC used in the present study—particularly due to the absence of coarse aggregates in their mix designs. However, it is reminded that the constituent materials and the mix proportions of ECC used by these authors were slightly different from those used in this study (See **Table 5.2** for a detailed comparison). Moreover, specimen preparation method, size, shape, and aspect ratio adopted by different authors were also varied. The specimens for quasi-static compression test conducted by Rong *et al.* (2010) were obtained from the recovered specimens of bending experiment, thus the residual stresses that remained in the specimen might affect the test measurement. In addition, Rong *et al.* (2010) and Chen *et al.* (2013) used inconsistent specimen shape and dimensions for quasi-static and dynamic tests. Both used rectangular prismatic specimens with aspect ratio ≥ 2 for quasi-static tests, but adopted cylinder specimens with aspect ratio of 0.5 for SHPB tests. On the contrary, consistent specimen geometry was adopted in the present study for both quasi-static tests (using conventional compression machine) and dynamic tests (using SHPB tests) so that a fair comparison of mechanical properties between the two conditions could be attained without the need to consider scaling effects.

Table 5.2 Comparison of constituent materials and mix proportion of ECC used by different authors

Authors	Type of cementitious materials	Binder	water/ binder	sand/ binder	HRWR/ binder	Fibre (%volume)
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present study	ECC, mortar	OPC (30%), GGBS (70%)	0.27	0.2	0.008	ECC: PVA (2%) Mortar: no fiber
Chen <i>et al.</i> (2013)	ECC	OPC (50-20%), GGBS (50-80%)	0.27	0.36	0.01	PVA (2%)
Rong <i>et al.</i> (2010)	mortar	OPC (40%), silica fume (10%), fly ash (25%), GGBS (25%)	0.15	1.2	0.02	No fiber

DIF Compressive Strength

The comparison of DIFs for compressive strength obtained from the present study, the past studies, and the MC2010 (fib, 2013) is presented in **Fig. 5.16**. It is evident that despite variations in mix design, specimen size, shape, and aspect ratio, the collated data showed a clear, consistent trend of a steep increase of compressive strength of ECC and mortar at high strain rate ranges. In addition, it is also shown that the DIFs for compressive strength of mortar (hollow markers) at high strain rates match quite closely to the extrapolated DIF from MC2010 (fib, 2013). On the other hand, the DIFs for compressive strength of ECC (solid markers) are generally lower than that obtained from *fib* (2013), but they tend to converge to the same values at strain rates beyond 1000/s. This preliminary finding suggests that the use of MC2010 (fib, 2013) for DIF formulas—and their extrapolated values—for strain rate below 1000/s can result in overestimation of ECC compressive strength.

The trend line for DIF compressive strength at strain rate beyond 100/s in **Fig. 5.16** can be expressed mathematically as **Eq. 5.1(a)** and **Eq. 5.1(b)** for ECC and mortar, respectively. Following the notations in MC2010, $\dot{\varepsilon}_c$ and $\dot{\varepsilon}_{co}$ is the dynamic strain rate and quasi-static strain rate ($= 30 \times 10^{-6}/s$), respectively.

$$\text{Mortar:} \quad DIF_{strength} = 0.0019 \left(\frac{\dot{\varepsilon}_c}{\dot{\varepsilon}_{co}} \right)^{0.4487} \quad (\text{Eq. 5.1(a)})$$

$$\text{ECC:} \quad DIF_{strength} = 0.0006 \left(\frac{\dot{\varepsilon}_c}{\dot{\varepsilon}_{co}} \right)^{0.5067} \quad (\text{Eq. 5.1(b)})$$

Fig. 5.16 DIF compressive strength of ECC and mortar

DIF Critical Strain

The critical strain in the present context is defined as the corresponding strain at the peak compressive strength. **Fig 5.17** shows the strain rate effect on the critical strain of ECC and mortar. Similar to the case of compressive strength, the critical strain data also suggest an increasing trend towards higher strain rates. Nevertheless, unlike the case of compressive strength, there are huge discrepancies between the DIFs for critical strain obtained from the experimental tests to the extrapolated DIF values from MC2010 (fib, 2013). Even in the case of mortar, it is shown that the former displays much steeper ramp than the latter at high strain-rate ranges. It is suspected that the deviation may be attributed to the absence of coarse aggregate in the mortar and ECC under investigation. The absence of coarse aggregate might cause ECC and mortar to experience greater deformation (i.e. larger strain) than normal concrete at the peak compressive strength. The conditions might be aggravated further by higher loading rates.

Fig. 5.17 DIF critical strain of ECC and mortar

The trend line equations for DIF critical strain at strain rate beyond 100/s are shown in **Eq. 5.2(a)** and **Eq. 5.2(b)** for ECC and mortar, respectively.

Mortar:
$$DIF_{critical\ strain} = 0.0118 \left(\frac{\dot{\epsilon}_c}{\dot{\epsilon}_{co}} \right)^{0.3597} \quad (\text{Eq. 5.2(a)})$$

ECC:
$$DIF_{critical\ strain} = 0.0011 \left(\frac{\dot{\epsilon}_c}{\dot{\epsilon}_{co}} \right)^{0.5033} \quad (\text{Eq. 5.2(b)})$$

Compared to the experimental data of DIF for compressive strength, the experimental data of DIF for critical strain show remarkable divergence. It is hypothesized that the variations in the cementitious matrix composition used between authors affect the strain characteristic more significantly than the strength characteristic. More tests and further analyses should be conducted to gain deeper understanding on this topic.

CHAPTER 6: CONCLUSION AND FUTURE WORK

6.1. Conclusion

Throughout its service life, a structure may be subjected to various kinds of loading, including the extreme ones, such as seismic, explosive or impact loads. A special structure (e.g. a military structure and a protective shelter), in particular, should be designed with a certain safety margin against these hazards. Unfortunately, the behaviour of structures under extreme loading conditions is the least understood by most designers and engineers due to its complex, time-dependent (dynamic) nature. Reinforced concrete—the most widely used construction material in the world—is recognized for its superior performance as a protective material (i.e. massive, relatively inert and durable). However, these capabilities are often restrained by its brittleness. To overcome this issue, fibres are often added to the concrete matrix. In recent years, there has been increasing interest towards research related to performance of concrete with fibre inclusions. Amongst these new materials, ECC can be considered as the most cost-efficient one since it is able to deliver outstanding performance with minimum fibre content.

The challenge to utilize ECC for protective structures is its unknown dynamic behaviour. While past studies were mainly focused on ECC behaviour under quasi-static conditions, a protective structure is expected to withstand high strain-rate loading (e.g. impact and blast). Using quasi-static data to predict ECC performance under dynamic conditions may lead to disastrous consequences as its behaviour may completely change under rapid loading rate. In the present study, ECC dynamic behaviour is investigated using the Split-Hopkinson Pressure Bar (SHPB). Two different specimen geometries, i.e. cylinder and cuboid, are also studied to assess the applicability of using cuboid specimens for SHPB tests—cuboid specimens are easier and faster to fabricate and are more suitable for optical measurement using high-speed imaging than cylinder specimens.

Another challenge of adopting ECC, especially in Singapore context where mineral resources like silica sand and fly ash are lacking, is its production using locally available materials. In the present study, alternative constituent materials to replace silica sand and fly ash are attempted. Silica sand is substituted by river sand, while fly ash is substituted by GGBS. Although the mechanical performance of ECC produced using these alternative materials is not as satisfactory as the mechanical performance of ECC produced using the original constituent materials, the adopted ECC still shows considerable ductility improvement compared to normal concrete.

The outcomes of the research can be summarized as follow.

1. Cylindrical and cuboidal specimens will show similar behaviour and comparable stress-strain relationship as long as low boundary friction at bar-specimen interfaces can be established throughout the tests. Nonetheless, in SHPB tests where the boundary friction is controlled by the use of lubricants, it is difficult to maintain consistent degree of minimum friction throughout the tests. When the specimens have undergone significant deformation and cracks start to form, the lubricant layer may be greatly compressed and may penetrate into the specimens through the cracks. At this stage, the boundary friction starts to increase and the test results from the cuboid specimens and the cylinder specimens begin to deviate from each other. This explains why the descending branches of the compressive stress-strain relationships from the cuboid and cylinder specimens differ. The discrepancies, however, are less noticeable in the case of ECC due to ECC ability to resist crack growth.
2. Observation on the final failure modes of the specimen under dynamic compression reveals that the cylinder specimens failed by the formation of splitting-tensile cracks, while the cuboid specimens failed by the formation of splitting-tensile cracks combined with diagonal or corner cracks. The orientation of the splitting cracks suggests that the specimen tends to expand in lateral direction due to Poisson's effect when being compressed. The diagonal cracks may prevail due to non-planar stress wave propagation that eventually compels the crack to grow at an angle in cuboid specimens. On the other hand, the corner cracks occur due to the accumulation of stresses in the sharp corner of cuboid specimens. The development of the diagonal and the corner cracks, which

consume considerable amount of energy, might be misinterpreted as an inherent mechanical property of the materials under compression. Therefore, to obtain results that reflect and represent a real uni-axial material behaviour, a cylindrical specimen is deemed a better geometrical shape than a cuboidal specimen.

3. A comparison of compressive dynamic behaviour between ECC and mortar can be described (1) quantitatively through the resulting stress-strain curves and (2) qualitatively through visual observation of the crack size and the crack distribution. From the comparison of the stress-strain curves, it is shown that mechanical behaviour of the two materials is comparable at small deformation, but starts to deviate from each other near the peak region. ECC is able to maintain its peak capacity over greater range of strains and displays more gradual decrease of the strain-softening branch than mortar owing to more stable crack propagation by means of the fibre-bridging action. This finding is consistent with the crack patterns that show the formation of multiple fine cracks in ECC specimen, but a single large crack in mortar specimen. Moreover, at the end of the test (i.e. when the specimen had been subjected to multiple wave reflections), the ECC specimens broke into few large fragments, but the mortar specimens crumbled into many small fragments. This further affirms the resiliency of ECC even under dynamic loading.
4. From the present study, it is also recognized that ECC is a strain-rate sensitive material similar to normal concrete. The mechanical compressive properties of ECC—i.e. the compressive strength and the critical strain—change as the strain rate changes. Using a compilation of experimental data from this study and other authors', the DIF for ECC can be established. The compressive strength data of ECC—despite varied constituent materials and different mix proportions adopted between authors—display a clear trend. Generally, it is found the DIF for compressive strength of ECC is smaller than that of mortar or normal. Thus, the use of DIF compressive strength formulation in MC2010 may lead to un-conservative predictions. On the contrary, the critical strain data of ECC is much scattered due to varied mix designs, hence further studies are needed to gain more comprehensive understanding of the effect of strain rate on mechanical properties of different ECC mixes.

6.2. Future studies

The mix proportion and the material constituent of ECC used in the present study were selected based on past experimental studies that had been adjusted to accommodate the scarcity of mineral materials in Singapore. Although the tensile ductility of ECC used in the present study is notably higher than that of mortar or normal concrete, it is still considered low compared to the tensile ductility of original ECC mixes. To enhance the tensile ductility performance of ECC, several different mix compositions should be further investigated.

Also, a comprehensive study on material model for ECC should be conducted in the subsequent study so that a complex structural engineering analysis—such as structures under impact and blast loading conditions—can be handled effectively and accurately through finite element algorithm. Since impact or blast experimental tests can be time-consuming, labour-intensive, and costly, a numerical approach is still considered as the most economical way to investigate impact- or blast-resistant mechanism in structural members. With robust finite element model, the numerical analyses can provide valuable insights that may be difficult to obtain experimentally (e.g. inertia effect, boundary friction effect, etc.).

Furthermore, understanding ECC dynamic responses under other kinds of loadings (i.e. tension, bending, shear, or torsion) is also of paramount importance. Nevertheless, it is presently difficult to conduct dynamic experimental studies for these types of loadings due to limited capability of present equipment to perform high-strain rate tensile experiment (typical hydraulic machine can only achieve a maximum strain rate of 0.1s⁻¹). Most high strain rate tensile testing experiments on cementitious composites utilized impact loading that generate tensile stress waves by reflecting the compressive stress waves (i.e. spalling or splitting tests). Unfortunately, these experimental techniques suffer a disadvantage as they are unable to produce complete stress-strain relationships of the test materials. Thus, there is a need to explore and to further investigate this matter.

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APPENDIX A: NUMERICAL SIMULATION OF SHPB TEST USING ANSYS/LS-DYNA

Since it was difficult to quantify the influence of radial inertia confinement effect from the experimental test data, the inertia effect should be examined by means of numerical simulation. To enhance the efficiency of the numerical analysis without jeopardizing its accuracy, a physical problem should be transformed into an idealized model—including only the essential features of the actual test. The idealized model to simulate the experimental test described in **Chapter 4** consisted of two main parts: the specimen and the pressure bars (i.e. the striker bar, the input bar, and the output bar) as shown in **Fig. A.1**. Despite more time consuming, three-dimensional (3D) numerical analysis was preferred over two-dimensional (2D) numerical analysis since 3D analysis was able to account for the triaxial non-linear material behaviour and to more realistically simulate the actual mass of the specimen, hence more accurately describing the inertial effect. Therefore, 8-node hexahedron solid elements for both the pressure bars and the specimen were utilized in this study.

Fig. A.1 Numerical model of SHPB test

The simulation was conducted using ANSYS/LS-DYNA—the model was built using ANSYS Parametric Design Language (APDL), but was executed by LS-DYNA solver. Since the finite element analysis with solid elements can be computationally expensive and time consuming, other means to enhance the computation efficiency without compromising its accuracy should be applied. These included adopting reduced integration technique with hourglass control, modelling only one quarter of the actual test configuration (which was possible due to double symmetry in geometry and loading systems of the test), and modelling the pressure bars using linear, elastic, isotropic material model (MAT 1—which was very cost efficient since it neither consumed much processing time nor required substantial storage for storing history variables).

On the other hand, the specimen was modelled using concrete damage model (MAT 72). Since the derivation of material constitutive relationship of ECC was still in rudimentary stage, only the mortar specimens would be simulated in the present study. The validated parameter values recommended by the LS-DYNA User's manual (LSTC, 2003) were adopted as a good starting input values. Since the purpose of the simulation at this stage was to qualitatively observe the effect of inertia in influencing the stress wave propagation in the specimen (and was not intended to provide precise quantification of radial inertia effect on measured mechanical properties of ECC and mortar), the adopted material model was deemed reasonable. Nonetheless, more comprehensive studies need to be conducted in the future to portray ECC and mortar behaviour more realistically.

The presence of radial inertia can be observed from the radial stress history of the specimen (dotted lines in **Fig. A.2**). As the specimen is loaded in axial direction, it tends to displace in outward radial direction, but is hindered from doing so due to inertial effect. This condition gives rise to radial stress that generates confining pressure (i.e. hydrostatic pressure) on the core of the specimen.

Fig. A.2 The time history of axial stress and radial stress in the central node of the specimen (a) square-sided specimen (b) circular-sided specimen

The increase of radial stress in turn is accompanied by the increase of axial stress. The radial stress history (dotted lines) and the axial stress history (solid lines with square marker) in **Fig. A.2** generally display similar curve trend throughout the test duration and they also reach their peak values at the same time, inferring significant correlation between the inertia-induced radial stress and the measured axial stress in the specimen. It should be noted that the concrete damage model adopted in the present study had considered the pressure-dependent behaviour of the cementitious materials. Also, the contact surface between the specimen and the pressure bars is assumed frictionless so that the simulation results are independent from the boundary restrain effect.

To further compare the inertia effect between the two specimen geometries, the radial displacement contours from the simulation results should be examined. **Fig. A.3** shows the radial displacement contour when the compressive wave propagates halfway along

the thickness of the specimen (noted that the same level of contours for both the square- and the circular- sided specimens are used for ease of comparison). **Fig. A.3 (a) and (b)** give an indication that the radial stress wave in a square-sided specimen propagates along the longitudinal axis of the specimen with a concave-shaped wave front, while that in a circular-sided specimen propagates with a planar-shaped wave front. In addition, **Fig A.3 (c)** also shows that the distribution of radial displacement over the specimen cross-section is less uniform in the case of square-sided specimens. As a result, the axial uniformity of the square-sided specimens is more difficult to achieve during high-rate deformation. Nonetheless, if the average value of axial stresses from all the elements in the cross section is considered, the resulting of stress-strain curves between the two geometries (**Fig. A.4**) will be similar.

In agreement to the stress-strain curves from experimental tests, the stress-strain curves from numerical analysis also show that the square-sided specimen displays more gradual decreasing branch than the circular-sided specimen. Unfortunately, the stress-strain curves obtained from the experimental test and the numerical test could not be directly compared since the parameter values of the concrete damage model adopted in the present study require further modifications and verifications to simulate the mortar or ECC behaviour accurately. Also, the boundary conditions at bar-specimen interfaces cannot be assumed perfect frictionless throughout the test.

(a)

Fig. A.3 The comparison of radial displacement contour between square-sided and circular-sided specimens (a) 3D view (b) 2D side view (c) 2D front view

Fig. A.4 The comparison of stress-strain curves between square- and circular-sided specimens from numerical analysis at strain rates about 700/s