Ductility of 3D printed concrete reinforced with short straight steel fibers

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Ductility of 3D printed concrete reinforced with short straight steel fibers

F. P. Bos, E. Bosco and T. A. M. Salet

Abstract

With the number of 3D printed concrete structures rapidly increasing, the demand for concepts that allow for robust and ductile printed objects becomes increasingly pressing. An obvious solution strategy is the inclusion of fibers in the printed material. In this study, the effect of adding short straight steel fibers on the failure behaviour of Weber 3D 115-1 print mortar has been studied through several CMOD tests on cast and printed concrete, on different scales. The experiments have also been simulated numerically. The research has shown that the fibers cause an important increase in flexural strength, and eliminate the strength difference between cast and printed concrete that exists without fibers. The post-peak behaviour, nevertheless, has to be characterised as strongly strain-softening. In the printed specimens, a strong fiber orientation in the direction of the filament occurs. However, this has no notable effect on the performance in the tested direction: cast and printed concrete with fibers behave similarly in the CMOD test. For the key parameters, no scale effect was found for the specimens with fibers, contrary to the ones without. Numerical modelling of the test by using the Concrete Damage Plasticity material model of Abaqus, with a Thorenfeldt-based constitutive law in compression and a customised constitutive law in tension, results in a reasonable fit with the experimental results.

1. Introduction

With digitally fabricated concrete (DFC) quickly outgrowing the laboratory phase, as showcased by recent examples such as a pedestrian bridge in Spain (3ders 2017a), a bicycle bridge in the Netherlands (Salet et al. 2018), an office hotel in Denmark (3dprinthuset 2017), and a workshop in Dubai (3ders 2017b), the quest for tensile capacity and ductility has become a major issue (Salet et al. 2017; Asprone et al. 2018) because conventional reinforcement techniques are either incompatible with DFC, or largely cancel out its potential benefits. Several innovative concepts have been presented that provide ductility and tensile capacity to printed concrete in an automated fashion, including robotised placement of mesh-reinforcement and automatically entrained cable reinforcement (Hack and Lauer 2014; Hack et al. 2015; Bos et al. 2017). External passive reinforcement and 3D printed steel reinforcement have also been suggested 2020 (Asprone et al. 2018; Mechtcherine et al. 2018). These concepts require further development to be able to fully appreciate their possibilities.

An obvious strategy to address this issue is to include fibers in the print material. In conventional concrete construction glass, steel, polymer, and other fibers have been used for decades for several purposes, for instance to reduce shrinkage cracking, but also to reduce or obviate the need for conventional reinforcement bars. In such a structural capacity, steel fibers are commonly used. Steel fiber reinforced concrete (SFRC) is particularly beneficial in applications in which conventional reinforcement is difficult to apply, for instance when a high reinforcement ratio is required, in underwater concrete, or in slender and geometrically complex elements or in areas of high load concentrations (e.g. due to localised connections). These latter two examples may typically occur in DFC.

Given these considerations, the current study explores the structural performance of SFRC applied in a particular DFC process, namely the ‘3D Concrete Printing (3DCP)’ Fused Deposition Modelling (FDM) process that is being developed at the Eindhoven University of Technology (TU/e) and is similar to facilities at several other research institutes and commercial enterprises (Bos et al. 2016; Khoshnevis and Dutton 1998; Khoshnevis 2004; Lim et al. 2012; Wu, Wang, and Wang 2016). First, the features of the printing with steel fibers in the
particular facility are discussed. Then, the theoretical framework is introduced. The experimental work, consisting of Crack Mouth Opening Displacement (CMOD) tests on differently sized cast and printed specimens, is subsequently presented. As part of this study, a continuous 2D numerical model was developed that has been used to recreate the experiments. The modelling issues are discussed, particularly with regard to the material model, as well as the correspondence between numerical and experimental results.

2. Fibers in 3D printed concrete

2.1. The use of structural fibers in concrete printing

The 3DCP facility is schematically shown in Figure 1. Steel fibers could not be applied in the dry mix, as the set-up makes use of a rotor-stator pump with rubber lining and narrow cavities that is unable to pass such fibers without blocking and damaging the pump. Therefore, an additional device was developed, capable of applying fibers near to the mixed material near the print nozzle (number 6 in Figure 1). In the future, it should be possible to adjust the fiber content by location in the print object to obtain optimised material use, as suggested (for non-printed applications) by Plückelmann, Song, and Breitenbücher (2017).

In the current study, short 6 mm straight fibers (Bekaert Dramix OL 6/.16) have been used as they were expected not to significantly impact the workability (Markovic 2006), and to be able to pass the nozzle opening without clogging as might occur with larger types of fibers (various nozzles are used, typically the opening is 10 x 40–60 mm²).

The addition of fibers to achieve ductility in 3D concrete printing is an obvious solution strategy that has been explored on a very small scale by Hambach and Volkmer (2017), who added 3–6 mm basalt, glass and carbon fibres to a printable mixture, and Panda, Chandra Paul, and Jen Tan (2017), who compared glass fibers of different lengths (3, 6 and 8 mm) and varying VOL% of fibres. Both studies reported a significant increase in flexural tensile strength, as well as an orientation effect of the fibers in the direction of the filament flow, but neither discussed the effects on ductility. The influence of several fibers on the flexural performance and interface strength has been studied by Nematollahi et al. (2018), while Weng et al. (2018) tested the flexural and compressive strength of a single mixture with poly-vinyl alcohol (PVA) fibers, and Zareiyan and Khoshnevis (2018), among other things, studied the influence of polypropylene (PP) fibers on the interface adhesion. Furthermore, printable engineered cementitious composites (ECCs) and strain-hardening cementitious composites (SHCCs) and are under development, as shown by Soltan and Li (2018) and Ogura, Nerella, and Mechtcherine (2018). These groups used PVA and high-density polyethylene (HDPE) fibers, respectively, and obtained strain-hardening performance.

2.2. Structural performance of concrete reinforced with straight steel fibers

As shown e.g. by Markovic (2006), it should be expected that normal strength concrete with short straight steel fibers will show increased tensile strength due to delayed macro-crack development. Furthermore, a strong strain-softening post-crack behaviour should be anticipated, as the fiber-matrix adhesion is insufficient to generate a pull-out resistance in the order of magnitude of the fiber strength, i.e. the fiber efficiency is ≪100%. The increased tensile strength, fracture toughness, and failure strain may nevertheless be beneficial for many practical applications, and improve structural performance in statically indeterminate structures.

The performance of fiber reinforced concrete (FRC) has been reported to depend on fiber properties (geometry, material, aspect ratio), matrix (cementitious material, filler and aggregates, water/cement ratio), as well as the matrix-fiber combination (fiber distribution, fiber orientation) (Markovic 2006; Naaman 2003). With regard to fiber orientation, it should be noted that, since the fiber resistance is governed by pull-out rather than yielding as for conventional reinforcement bars, a

![Figure 1. Schematic representation of TU/e 3DCP set-up. Steel fibers can not be pumped, and should therefore be added to the filament near the print head (6).](image)
full parallel alignment of the fiber in the direction of the principle tensile stress is not necessarily beneficial. Results found in literature are not entirely consistent on this issue. Several studies found an increase of resistance up to 20% for inclination angles of up to 60° (Van Gysel 2000; Robins, Austin, and Jones 2002), whereas another work found an important decrease of resistance leading up to 50% at 60° (Bartos and Duris 1994). Yet, others noted that the post-fracture resistance and energy absorption significantly reduces for inclination angles over 30° (Banthia and Trottier 1994; Armelin and Banthia 1997; Svec et al. 2012), supported by a study on the effect of the magnetic orientation of fibers (Wijffels et al. 2017). When corrected for a fixed limited value of fiber slip, several studies indicate an optimal pull-out resistance is reached at inclination angles of 10–15° (Markovic 2006). It is conceivable the effect on inclination angle is not independent of fiber geometry, as the resistance of deformed fibers is governed by the plastic deformation required for pull-out. Judging from the variety of results found in literature, it is hard to predict whether the notable orientation of fibers found by both Hambach and Volkmer (2017) and Panda, Chandra Paul, and Jen Tan (2017) in 3DCP due to the concrete flow, will be beneficial to the post-fracture performance or not. To assess the structural performance of FRC, RILEM (Rilem Technical Committees 2002) has proposed the use of the Crack Mouth Opening Displacement (CMOD) test. This test has been codified in the EN 14651 (Nederlands Normalisatie Instituut 2007) and was adopted by the fb Model Code 2010 (International Federation for Structural Concrete 2013) as the standard method to characterise FRC for structural calculations. In this standardised 3-point bending test, the increase of the notch width in a beam, known as the crack mouth opening, is measured during load application and used to obtain an overall tensile strength value as well as an indication of residual resistance after the Limit of Proportionality (LOP) has been reached. The residual resistance \( f_{Rj} \) is determined at prescribed levels of CMOD. Here, \( j = 1, 2, 3, 4 \) which corresponds to 0.5, 1.5, 2.5, and 3.5 mm of crack mouth opening respectively (e.g. CMOD = 2.5 mm). The residual resistances \( f_{R1} \) and \( f_{R3} \) are particularly important, as the fb Model Code 2010 uses these values to classify the performance of a particular FRC composition. Both the LOP and residual strengths are calculated through linear elastic theory, as in:

\[
\begin{align*}
  f'_{cL} & = \frac{3F_L}{2bh_{sp}} \\
  f_{Rj} & = \frac{3F_j}{2bh_{sp}}
\end{align*}
\tag{1}
\]

with \( F_L \) force at the limit of proportionality, and \( F_j \) the force at subsequent prescibed CMOD levels 0.5, 1.5, 2.5 and 3.5 mm. The geometrical features of the specimen (the height \( h_{sp} \), the width and the length \( l \)) are defined in Figure 3.

As the behaviour after fracture is generally highly non-linear, and often strain-softening, particularly the calculation of residual strength should be considered as an apparent value.

The presence of the notch eliminates edge effects and ensures the development of a single localised crack that can be measured unambiguously. However, it influences the flexural resistance in comparison to unnotched specimens, which obviously, are more similar to actual applications. Contrary to concrete without fibers where the notch induces a peak stress that leads to earlier failure, the notch has actually been reported to have a beneficial effect in FRC, particularly with non-straight fibers (Stähli and Van Mier 2004). Other studies have shown less pronounced results for FRC with straight fibers (Chanvillard and Rigaud 2003). Even though these issues should be heeded when translating experimental results to an application environment, the notched test has been used in this study for its practical merit of a predictable crack location and broad acceptance.

### 2.3. Scale effect

In this study, experimental tests have been performed on cast FRC specimens of the size specified by the EN 14651, as well as on cast and printed FRC specimens scaled down with a factor of 0.27, to match the scale of normal strength concrete (NSC). This should be attributed to the increased brittleness. They furthermore concluded that an increase in size led to a decrease in flexural performance, but an increase in fiber volume content reduced the sensitivity to this effect in SFRC and amorphous metallic FRC. In concretes of higher ductility, the size effect seems to disappear completely. Lepech and Li (2003) found no size effect in a range of tested Engineered Cementitious Composite beams without and with conventional reinforcement (ECC and R/ECC). Mahmuda, Yang, and Hassan (2013) concluded the size effect in ultra-high performance fiber reinforced...
Concrete (UHPFRC) beams is small. Considering that the scale effect is related to the strain energy release on the one hand, and the fracture surface energy on the other, this is in line with expectations.

A study by Giaccio, Tobes, and Zerbino (2008) was particularly aimed at the scale effect in the CMOD test, and intended to determine whether smaller specimens could be used to characterise FRC behaviour, arguing that this would simplify experimental testing and moreover corresponds better to some practical uses. They applied a scale factor of 0.7, not only to the EN 14651 specimen size, but also to the CMOD values at which relevant stress values are recorded (e.g. the strength fR1 was recorded at a CMOD1 of 0.5 mm × 0.7 = 0.35 mm). When this correction was applied, it was concluded that, as long as the fiber length and aggregate size are compatible with the mold, the proposed downsampling was allowable.

The EN 14651 allows testing of samples with aggregates of up to 32 mm in size, and fiber lengths of up to 60 mm. The applied material Weber 3D 115-1 has a maximum grain size of 1 mm, and the length of the applied fibers is 6 mm. Therefore, the ratios of the maximum to applied aggregate size and fiber length are significantly smaller (0.03 and 0.1, respectively) than the scale factor (0.27).

For several reasons, scaling down the specimen size in this study, was considered desirable as well as justifiable. Most importantly, the reduced specimen size results in section dimensions that bear resemblance to the sections of elements printed in the TU/e 3DCP facility. Testing reduced size specimens therefore corresponds best with the actual intended use. Furthermore, it results in a significantly improved research and material efficiency. Regardless of these considerations, the scale effect should be small anyway, as the concrete quality is low and a significant VOL% of fibers is applied.

3. Experimental research

3.1. Experimental set-up and test series

Three CMOD tests have been performed: CMOD-1 on cast, full scale specimens, CMOD-2 on cast, downsized specimens, and CMOD-3 on printed, downsized specimens. For each CMOD test, one set of specimens without and one set with fibers has been tested. With the exception of one, each set consisted of 3 specimens. Figure 2 schematically shows the specimen types, and Figure 3(a) details the experimental set-up. In Figure 3(b), a printed specimen is shown during testing, in the scaled down version of the test set-up.

CMOD-1 was performed in accordance with the EN 14651. However, given the typical size of a filament section (which roughly corresponds to the size of the nozzle opening), the EN 14651 specimen dimensions are unsuitable to evaluate the performance of printed concrete from the 3DCP process. The process is intended for more filigree structures, which should be reflected in the dimensions of experimental specimens. Therefore, the set-up and specimens of CMOD-2 and -3 were scaled down by a factor of 40/150 (≈ 0.27), so that the specimen section size w × h measures 40 × 40 mm². The nominal specimen dimensions are given in Table 1.

In all tests, the CMOD was measured with a clip gage and the vertical displacement with (a) linear variable differential transformer(s) (LVDT(s)). In CMOD-1, one LVDT was mounted on an aluminium rod connected to the specimens at mid-height above the supports, and measured the vertical displacement of a strip glued onto the specimen directly under the load point. In CMOD-2 and -3, this set-up was impossible due to the dimensions of the specimens. Alternatively, two LVDTs were used to measure the vertical displacement of glued on strips at each side of the crack mouth opening, and averaged.

The tests were performed on an Instron universal test rig, controlled by the crack mouth opening displacement, which was set at 0.05 mm/min until CMOD = 0.1 mm was reached, and then raised to 0.2 mm/min. The tests were stopped at specimen failure (without fiber) or when a CMOD ≥ 4 mm had been reached (with fiber).

3.2. Material

All specimens were prepared with Weber 3D 115-1 print mortar as the matrix material. This mixture was custom designed for the 3DCP process by Saint Gobain Weber Beamix and is globally described by Bos et al. (2016). Some material properties derived from uni-axial tension tests (Slager 2017) are given in Table 2.

Bekaert DRAMIX ® OL 6/16 high strength straight steel fibers were added to the B-sets of specimens in a quantity of 150 kg/m³ (corresponding to 2.1 VOL%). The basic fiber properties are provided by Bekaert (2017), and listed in Table 3. Table 4 provides an overview of specimen sets.

3.3. Specimen preparation

The material for CMOD-1 was mixed in a standard gravity mixer, with a water/mixture ratio of 0.2. The form work was initially filled for 90% (in the casting order specified by the EN 14651) and compacted for 10 s on a vibrating table, after which the remaining 10% was added and the specimen was compacted again for
10 s. The top was scraped flat. The specimens were demoulded after 24 h and stored under water for the rest of the curing period. The notch was applied with a saw one day before testing. The specimens were tested at an age of 28 days.

The specimen preparation of the CMOD-2 specimens was similar. However, instead of a gravity mixer, a hand mixer in a bucket was used due to small amount of material required.

The printed specimens of CMOD-3 were prepared with a beta version of the fiber addition device. Practically, this means the fibers were added to the mix that was prepared as for CMOD-2. Subsequently, the wet mix with the fibers was introduced in a screw-pump

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**Figure 2.** Schematic comparison of specimen types: 1A – full scale, cast, no fibers; 1B – full scale, cast, fibers; 2A – reduced scale, cast, no fibers; 2B – reduced scale, cast, fibers; 3A – reduced scale, printed, no fibers; 3B – reduced scale, printed, fibers.

**Figure 3.** CMOD test set-up: (a) general scheme based on the EN 14651, and (b) printed specimen being tested in scaled down version of CMOD test.
Table 1. Nominal specimen dimensions for each CMOD test.

<table>
<thead>
<tr>
<th>CMOD</th>
<th>b = h [mm]</th>
<th>h_{sp} [mm]</th>
<th>l_{sp} [mm]</th>
<th>h_{ip}/l_{ip}</th>
<th>x [mm]*</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>150</td>
<td>125</td>
<td>500</td>
<td>4</td>
<td>2</td>
</tr>
<tr>
<td>2</td>
<td>40</td>
<td>33</td>
<td>130</td>
<td>4</td>
<td>2</td>
</tr>
<tr>
<td>3</td>
<td>40</td>
<td>33</td>
<td>130</td>
<td>4</td>
<td>2</td>
</tr>
</tbody>
</table>

* the notch width could not be scaled.

Table 2. Weber 3D 115–1 experimentally obtained material properties (without fibers, uni-axial tension test).

<table>
<thead>
<tr>
<th>Property</th>
<th>Symbol</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Compressive strength, mean value</td>
<td>f_{cm}</td>
<td>23.2</td>
<td>N/mm²</td>
</tr>
<tr>
<td>Compressive strength, 0.95 characteristic value</td>
<td>f_{0.95}</td>
<td>20.7</td>
<td>N/mm²</td>
</tr>
<tr>
<td>Ultimate strain</td>
<td>l</td>
<td>0.0035</td>
<td>–</td>
</tr>
<tr>
<td>Tensile strength, mean value</td>
<td>f_{tm}</td>
<td>1.49</td>
<td>N/mm²</td>
</tr>
<tr>
<td>Specfic Fracture energy</td>
<td>G_{f}</td>
<td>0.031</td>
<td>N/mm²</td>
</tr>
<tr>
<td>E-modulus, tangent</td>
<td>E_{0}</td>
<td>17,336</td>
<td>N/mm²</td>
</tr>
<tr>
<td>E-modulus, secant</td>
<td>E_{oo}</td>
<td>25,717</td>
<td>N/mm²</td>
</tr>
</tbody>
</table>

Table 3. Bekaert Dramix OL 6/.16 steel fiber properties, according to Declaration of performance.

<table>
<thead>
<tr>
<th>Property</th>
<th>Symbol</th>
<th>Value</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Length</td>
<td>l</td>
<td>6</td>
<td>mm</td>
</tr>
<tr>
<td>Diameter</td>
<td>d</td>
<td>0.15</td>
<td>mm</td>
</tr>
<tr>
<td>Aspect ratio</td>
<td>l/d</td>
<td>40</td>
<td>–</td>
</tr>
<tr>
<td>Tensile strength</td>
<td>f_{e}</td>
<td>3000</td>
<td>N/mm²</td>
</tr>
<tr>
<td>E-modulus</td>
<td>E</td>
<td>200,000</td>
<td>N/mm²</td>
</tr>
</tbody>
</table>

device that was connected to a hose and print nozzle that was manually moved over a deposition plate to create layered specimens in a fashion similar to the automated 3DCP process. The specimens were stored under water after initial hardening and sawed to size before testing, as their dimensions exceeded the 40 × 40 mm² dimensions.

3.4. Experimental results

As reported by other researchers, a strong fiber orientation in the direction of the print filament was visually observed in the printed specimens, compared to the cast ones. The fibers are thus generally oriented parallel to the main bending stresses in the CMOD test. This is shown by Figure 4(a–c), showing a spare piece of fiber reinforced printed material, sawed perpendicular (Figure 4(a)) and parallel (Figure 4(b,c)) to the print direction.

The load-CMOD curves that directly result from the tests have been recalculated into stress-CMOD curves through Equation (1). Figures 5–7 show the stress-CMOD curves for CMOD-1, -2, and -3, respectively, with and without fibers.

The flexural tensile strength f_{t}, LOP f_{t} and residual strengths f_{R,1} and f_{R,3} according to the EN 14651, are listed in Table 5. For the scaled down CMOD-2 and CMOD-3 tests, also the residual strengths at scaled CMOD levels are given (f_{R,1}/f_{R,3} and f_{R,3}/f_{R,1}). These have been also indicated in the graphs. The size dependent specific fracture energy G_f, calculated over the displacement from the peak strength (fracture onset) onwards, is also included in Table 5. Figure 8(a,b) summarize the key properties in a comparative column chart.

Finally, Table 6 lists the average post-cracking classification of the fiber reinforced series according to the fib Model Code 2010. For the scaled down specimens, a characterisation both based on f_{R,3}/f_{R,1} as well as on f_{R,3}/f_{R,1} is provided.

3.5. Discussion

3.5.1. Effect of fibers and printing

The experimental results show that the addition of fibers significantly increases the flexural strength (Figure 8(a)). The increase is most striking for the printed specimens, in which it is twice as high as in the cast, full-scale specimens, reaching over 500%. This is mainly due to the low flexural bending strength of the printed specimens without fibers, which is only about half the strength of the cast specimens (see further discussion below). In all cases, the fibers introduce a residual strength in the specimens that is modest in comparison to other fiber reinforced concrete compositions, but nevertheless a significant improvement over concrete without fibers. The obtained post-fracture behaviour is strongly strain-softening, to an extent that a classification according to the fib Model Code 2010 is impossible (f_{R,3}/f_{R,1} < 0, 5), if the CMOD for j = 1 and j = 3 is not adjusted for scale.

The increase in flexural and residual strength, obviously, also translates in an importantly enlarged specific fracture energy (Figure 8(b)), by a factor of >30 in the cast full-scale elements, up to a factor of >100 for the scaled-down specimens (printed and cast).

For specimens without fibers, a significant decrease in flexural strength was found for printed specimens in comparison to cast ones (36%, compare series 2A and 3A,
Figure 4. Sawn open spare piece of fiber reinforced, printed concrete. Figure (a) was sawn open perpendicular to the print direction. The fibers are slightly bended due to the sawing. In Figures (b and c), showing the parallel sections, the fibers appear as long lines, indicating orientation of the fibers in the print direction.

Figure 5. Stress-CMOD curves for CMOD-1A and -1B (blue tones and red tones, respectively).

Figure 6. Stress-CMOD curves for CMOD-2A and -1B (blue tones and red tones, respectively).
Figure 8(a) and Table 5). As the specimen orientation in the tests was such that the results were not influenced by the layered structure of the printed specimens, this difference is expected to be caused by the lack of compaction in the printing process. Remarkably though, the flexural strength reduction vanishes for cast and printed concrete with fibers (+6%, compare 2B and 3B). Likewise, the specific fracture energy and residual strengths $f_{R,1}$ and $f_{R,3}$ are within small margins. The fiber orientation caused by the printing process does not seem to have a positive or negative effect compared to randomly oriented fibers, at least for the tested direction.

Thus, the addition of fibers to this printed concrete shows significant benefits. It eliminates the detrimental effect of a lack of compaction. Even though the material behaviour is still strongly strain-softening, the increase in flexural strength and fracture energy has advantages, particularly for secondary structural applications (e.g. façade elements) and connections (redistribution of peak stresses).

3.5.2. Scale effect

Contrary to expectations, this study found the flexural bending strength of the cast specimens without fibers was reduced for the scaled-down series (26% reduction, compare series 1A and 2A), rather than enlarged. This is possibly caused by the geometry and accuracy of the notch which had a larger influence on the smaller specimens. For the cast specimens with fibers, a reduction was also found, but it was much smaller (11%, compare 1B to 2B) and solely caused by the underperformance of a single specimen — see Figure 6. The small number of specimens in each series does not allow for any final conclusions on this issue.

Table 5. Experimental results.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>CMOD Test properties</th>
</tr>
</thead>
<tbody>
<tr>
<td>Spec.</td>
<td>$f_{LOP}$</td>
</tr>
<tr>
<td>1A (cast, full size, no fibers)</td>
<td>2A.1</td>
</tr>
<tr>
<td>1A.2</td>
<td>1.91</td>
</tr>
<tr>
<td>Average</td>
<td>1.79</td>
</tr>
<tr>
<td>1B (cast, full size, fibers)</td>
<td>1B.1</td>
</tr>
<tr>
<td>1B.2</td>
<td>6.25</td>
</tr>
<tr>
<td>1B.3</td>
<td>5.95</td>
</tr>
<tr>
<td>Average</td>
<td>6.28</td>
</tr>
<tr>
<td>2A (cast, scaled, no fibers)</td>
<td>2A.1</td>
</tr>
<tr>
<td>2A.2</td>
<td>1.91</td>
</tr>
<tr>
<td>Average</td>
<td>1.79</td>
</tr>
<tr>
<td>2B (cast, scaled, fibers)</td>
<td>2B.1</td>
</tr>
<tr>
<td>2B.2</td>
<td>6.32</td>
</tr>
<tr>
<td>2B.3</td>
<td>6.45</td>
</tr>
<tr>
<td>Average</td>
<td>5.60</td>
</tr>
<tr>
<td>3A (printed, scaled, no fibers)</td>
<td>3A.1</td>
</tr>
<tr>
<td>3A.2</td>
<td>1.12</td>
</tr>
<tr>
<td>3A.3</td>
<td>1.37</td>
</tr>
<tr>
<td>Average</td>
<td>1.14</td>
</tr>
<tr>
<td>3B (printed, scaled, fibers)</td>
<td>3B.1</td>
</tr>
<tr>
<td>3B.2</td>
<td>6.46</td>
</tr>
<tr>
<td>3B.3</td>
<td>4.86</td>
</tr>
<tr>
<td>Average</td>
<td>5.96</td>
</tr>
</tbody>
</table>

*The scaled residual strength values do not apply to the full scale specimens.*
The residual strength at \( j = 1 \) is quite similar for both the full and reduced scale. At \( j = 3 \), the residual strength in the reduced scale specimens is slightly larger.

The specific fracture energy \( G_f \) of the specimens without fiber is highly scale dependent (compare 1A and 2A). The obtained value from the full scale test is comparable to the value obtained earlier from uni-axial tensile testing (Table 2), but for the scaled down specimens, it is only half. This can be explained from the definition of the size dependent specific fracture energy \( G_f \) in a CMOD test. This can be calculated as the energy exerted by the external load divided by the fracture surface area perpendicular to the load direction (Abdalla and Karihaloo 2003), i.e.

\[
G_f = \frac{1}{h_p b} \int F d \delta
\]  

From Eq. (2) it follows that \( G_f \) is largely determined by the product of the flexural strength and the corresponding displacement, as the post-fracture branch is only very short. Based on standard mechanics equations \((\sigma = \frac{3F}{2bh^2} ; d = \frac{F l}{48Ebh^3})\), it can be determined that an increase in scale with a factor 2, leads to a section \( A = bh \) that is 4 times as high, but also results in an increase of \( Fd \) with a factor of 8, i.e. a net increase with a factor \( 8/4 = 2 \) – if only the peak would determine \( G_f \).

The fiber reinforced specimens, on the other hand, show hardly any scale effect with regard to specific fracture energy (compare 1B and 2B), because its value is mainly determined by the post-fracture behaviour, rather than the peak strength. Note finally that edge effects in the experimental tests influence the strain energy release that drives crack growth over a distance known as the ligament length \( a_l \), creating an additional source of size-dependency (Abdalla and Karihaloo 2003).

Although the residual strength ratio \( f_{R,3}/f_{R,1} \) is higher for the smaller fiber reinforced specimens due to a higher value of \( f_{R,3} \), this does not result in a different classification of post-fracture behaviour. However, this changes when scaled down values for \( j = 3 \) (CMOD = 2.5 mm \( \cdot \) 0.27 = 0.675 mm) and \( j = 1 \) (CMOD = 0.5 mm \( \cdot \) 0.27 = 0.135 mm) are applied, as suggested by Giaccio, Tobes, and Zerbino (2008). In that case, an ‘a’ classification is found for the scaled down specimens.

### 4. Numerical modelling

Even though the printed SFRC obtained in this study shows strongly strain softening behaviour, the increased tensile strength and ultimate strain can have merit in structural applications, for instance in the design of structural joints. In order to assess the structural performance of this material in arbitrary geometries, a suitable calculation method has to be determined. Therefore, the experiments were simulated through Finite Element Modelling (FEM) and evaluated on their agreement with the experimental results.

#### 4.1. Material model

The combination of isotropic damage and plasticity is a frequently used modelling framework to describe the complex non-linear response of concrete. Several models in the literature have been developed along this strategy, allowing to capture the failure response of the material both in tension and in compression – see for instance (Lubliner et al. 1989; Peerlings et al. 1998; Voyiadjis, Taqieddin, and Kattan 2008; Nguyen (2010)).

![Figure 8](image-url)
and Korsunsky 2008; Grassl and Jirasek 2006; Poh and Swaddiwudhipong 2009). In this paper, the Concrete Damaged Plasticity (CDP) model proposed by Lubliner et al. (1989) has been adopted. This is used to describe both concrete with and without fibres, in the assumption that, when fibres are present, they are uniformly distributed and the material can be thus considered to be homogenous. The CDP model is available as a material model in the commercial software Abaqus, with the option of implementing separate user-defined hardening or softening post-cracking curves for compression and tension that come into effect after a linear elastic branch.

4.2. Constitutive relation in compression

A commonly used constitutive law for concrete in compression is the relation proposed by Thorenfeldt, Tomaszewicz, and Jensen (1987) given as

\[ \sigma_c(\varepsilon_c) = f_{cm} \frac{\varepsilon_c}{\varepsilon_{c1}} \left( \frac{n}{n - (\varepsilon_c/\varepsilon_{c1})^k} \right) \]

with:

\[ n = 0, 80 + f_{ck}/17 \quad \text{and} \quad k = \begin{cases} 1, & \text{if } 0 < \varepsilon_c < \varepsilon_{c1} \text{, where } \varepsilon_{c1} \text{ is the compressive strain at the peak stress.} \\
0, 67 + f_{ck}/62, & \text{if } \varepsilon_c \geq \varepsilon_{c1} \end{cases} \]

It consists of two quadratic branches, before and after cracking. As the CDP model includes a linear elastic pre-cracking branch, rather than a quadratic one, the Thorenfeldt law cannot be incorporated completely. Thus, in the CDP model three branches were defined in compression, as illustrated by Figure 9: (i) a linear elastic first branch with a limit at the characteristic compressive strength, (ii) a quadratic hardening branch up to the mean compressive strength and corresponding to the first part of the Thorenfeldt law, and (iii) a quadratic softening branch after the mean compressive strength that corresponds with the post-cracking part of the Thorenfeldt law. The input values were based on the values listed in Table 2, and have been used as the constitutive relation in compression for all the analyses (with and without fibers). Although the compressive properties would be influenced by the presence of fibers, this is not expected to significantly influence the response of the FE-model as it is governed by tensile failure.

4.3. Constitutive relation in tension

Several constitutive laws have been proposed to model the behaviour of concrete in tension. These essentially require two input data: the (size-independent) specific fracture energy \( G_F \) and the shape of the stress-strain softening branch (Rots et al. 1985). The specific fracture energy \( G_F \) can be defined as the integral of the stress-separation relation

\[ G_F = \int \sigma \, dw \]  

Here, \( \sigma \) and \( w \) represent the traction and the crack opening in the normal direction, respectively. The size-independent specific fracture energy \( G_F \) can be obtained from the size-dependent one \( \sigma \) by performing a set of experiments on test specimens of different scales or with different notch sizes (Abdalla and Karihaloo 2003).

In continuum damage models, as the CDP used in this analysis, the crack is smeared over a band of a certain width \( w_c \). Within a finite element setting, this is therefore related to the element size. In order to avoid dependency of the results on the selected discretization, the fracture energy must be released over this width, i.e. \( G_F \) is constant and does not depend on the finite element mesh (Rots et al. 1985; Bažant and Cedolin 1979; Hordijk 1991). Under this premise, the assumption is made here that the strain is constant along the crack band, yielding

\[ w = w_c \varepsilon_f \]  

This assumption has been proven to hold for lower order finite elements and for particular cases of symmetry (Rots et al. 1985), consistently with the numerical test presented in this paper. For more general loading conditions or higher order finite element types, assumption (5) does not generally hold. Non-local/gradient enhanced softening models (Peerlings et al. 1998; Poh and Swaddiwudhipong 2009) are more appropriate in the latter situation.

As for the strain-softening curve, for plain concrete, exponential functions are frequently used (Hordijk 1991; Cornelissen et al. 1986; Krätzig and Pölling 2004), as they have showed good agreement with experimental results. For two reasons, however, these cannot be used...

\[ \text{Figure 9. Thorenfeldt material model compared to the CDP model in Abaqus, in compression.} \]
for fiber reinforced concrete. First, the specific fracture energy is much higher than for plain concrete and therefore it would require to be established separately. Second, contrary to plain concrete, there is no generally accepted continuous post-fracture function for fiber reinforced concrete (Awinda, Chen, and Barnett 2016), because this would highly depend on the specifics (fiber type, volume, etc.) of any FRC. Therefore, the tensile post-fracture behaviour was modelled by inserting it in the CDP model in a tabular format, with values for stress and strain.

In this regard, the fictitious strain $\varepsilon_f$ in the crack was determined according to assumption (5), taking the crack opening $w$ to be equal to the experimentally acquired CMOD values and the crack band width $w_c$ to be equal to the characteristic mesh size, $l_c = \sqrt{A_e}$ with $A_e = A_{tot}/n_e$, the average element area (total mesh area divided by the number of elements).

The stress values for the tabular input have been determined with linear elastic mechanics through relations Eq. (1), as used by the fib Model Code 2010 to obtain the (residual) strength levels for values of CMOD. Although the stress at the specimen edge can be calculated from linear elastic mechanics up to fracture onset, the post-cracking stress will be distributed over the height in a non-linear and a principally undeterminable fashion. Also, it should be noted that this approach is the opposite of the spectrum compared to the determination of the specific fracture energy $G_f$ through Eq. (2), which assumes a constant distribution of fracture energy over the section height. It will depend on the actual crack mouth opening which approach resembles the actual distribution state more closely. Depending on the separation and the material behaviour, the actual stress at the outer edge can easily be 50% or less of the linear elastic calculated value. Nevertheless, the fib Model Code 2010 provides guidelines to recalculate these values into general constitutive laws that can be used in structural modelling. These approaches are elaborated by Di Prisco, Colombo, and Dozio (2013) and depend on stress distributions, structural characteristic length ($l_c$, essentially identical to the crack band width $w_c$), and Young’s modulus – all of which have to be assumed and are functions of the strain. Similar reservations have been noted by Singh (2017).

Considering the above remarks, the numerical modelling of general structural geometries should ideally be based on a constitutive law derived from direct uni-axial tensile tests (as e.g. performed by Mahmuda, Yang, and Hassan 2013), because this results in a less ambiguous apparent stress-separation relation. Awinda, Chen, and Barnett (2016) also came to this conclusion, but in reference to Petersson (1981) note that tensile tests are in general difficult to perform and go on to argue that modelling based on material properties obtained from flexural tests is allowable to simulate those tests (in that case in a discrete cracking model). This approach was adopted in this study. Note that even uni-axial tensile tests do not result in pure material properties: Rots and de Borst (1989) showed that asymmetric crack propagation under uni-axial tension has consequences for the derivation of stress–strain relations.

4.4. Geometry and boundary conditions

The experiment was numerically modelled in 2D, using the commercially available FE-code Abaqus. As experimental research Giaccio, Tobes, and Zerbino (2008) has shown the specimen thickness has no notable effect on the flexural performance, the application of a 2D model was considered to be appropriate.

The CMOD-1, CMOD-2 and CMOD-3 experiments were modelled, with their respective 2D geometries (which are identical for CMOD-2 and -3). The following boundary conditions apply. The displacement of the left bottom corner is fixed in the vertical and horizontal directions; the right bottom corner is vertically constrained. The loading was applied as a prescribed displacement at the top edge above the notch, using the relation between CMOD and vertical displacement given by the EN 14651.

To mesh the geometries, bilinear plane stress elements with reduced integration (CPS4R) were used. The mesh properties are given in Table 7. A bias-factor of 1 was applied to minimise the influence of non-constant mesh element sizes.

4.5. Results and discussion

Figure 10(a–d) show the deformed configuration of the CMOD-1B (full scale, cast, with fibers) test. The contour plot refer to the $\sigma_{xx}$ field. The crack localisation and development is clearly visible, and follows a trend significantly close to those of the experimental results.

Figures 11 and 12 illustrate the stress–strain curves (continuous line) obtained from the numerical simulations, for both the specimens without and with fibers. The average experimental results are shown for

<p>| Table 7. Applied meshes and properties. |</p>
<table>
<thead>
<tr>
<th>Geometry</th>
<th>Bias factor</th>
<th>$l_c$ [mm]</th>
<th>$n_e$ [-]</th>
<th>$A_{ave}$ [mm$^2$]</th>
</tr>
</thead>
<tbody>
<tr>
<td>CMOD-1</td>
<td>1</td>
<td>1.78</td>
<td>24,528</td>
<td>3.056</td>
</tr>
<tr>
<td>CMOD-2</td>
<td>1</td>
<td>0.91</td>
<td>6,457</td>
<td>0.989</td>
</tr>
<tr>
<td>CMOD-3</td>
<td>1</td>
<td>0.91</td>
<td>6,457</td>
<td>0.989</td>
</tr>
</tbody>
</table>
comparison with dashed lines. In general, the numerical results are fairly capturing those of the experimental tests.

In the numerical model, the peak stress is reached at a slightly lower value of CMOD than in the experimental test. This can be explained by the fact that the CDP model assumes a linear-elastic trajectory up to fracture, whereas in reality micro-cracks reduce the stiffness before fracture onset. The flexural strength peak is found accurately, which is to be expected as a linear elastic stress distribution over the specimen height is still valid at this point.

Beyond the peak, the numerical curves generally overestimate the trend of the experimental ones, especially in the case of fibre-reinforced specimens – Figure 12. This discrepancy grows with increasing values of CMOD. The overestimation is to be attributed to the modelling assumptions discussed earlier. For the fibre-reinforced material, translating the experimental results in terms of load-CMOD curves into stress–strain relations, by assuming relation (5) to hold, may introduce a first source of mismatch. Additionally, taking a linear elastic stress distribution over the beam height overestimates the

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**Figure 10a–f.** Deformed configurations predicted from the FEM model (scale factor 10). The contour variable shows the stress $\sigma_{xx}$ distribution in CMOD-1B.

**Figure 11.** Stress-CMOD curves for CMOD-1A, -2A, and -3A (full size, scaled down cast and scaled down printed, respectively, without fibers), average experimental (dashed lines) and numerical (continuous lines).

**Figure 12.** Stress-CMOD curves for CMOD-1B, -2B, and -3B (full size, scaled down cast and scaled down printed, respectively, with fibers), average experimental (dashed lines) and numerical (continuous lines).
stress level in the ultimate fiber once the actual stress distribution moves towards a more plastic like behaviour. Thus, this effect grows stronger for higher values of CMOD.

The presented numerical modelling method, thus, results in a reasonably accurate simulation of the CMOD test up to a moderate extent after fracture. To improve the description in the post-peak response, it would be necessary to base the constitutive model on data obtained from uni-axial tensile tests and consistently to use more tailored damage models. This will be considered in future work.

5. Conclusions

The effect of adding short straight steel fibers on the failure behaviour of Weber 3D 115-1 print mortar has been studied through several CMOD tests on cast and printed concrete, on different scales. The experiments have also been simulated numerically. The research has shown the fibers cause an important increase in flexural strength, and eliminate the strength difference between cast and printed concrete without fibers. The post-peak behaviour, nevertheless, has to be characterised as strongly strain-softening. In the printed specimens, a strong fiber orientation in the direction of the filament occurs. However, this has no notable effect on the performance in the tested direction: cast and printed concrete with fibers behave similarly in the CMOD test. For the key parameters, no scale effect was found for the specimens with fibers, contrary to the ones without fibers. Thus, it appears a characterisation of material performance based on a scaled down version of the standardised CMOD test can be justified. Numerical modelling of the test by using the Concrete Damage Plasticity material model of Abaqus, with a Thorenfeldt-based constitutive law in compression and a customized constitutive law in tension, based on tabulated stress and strain values determined from the CMOD test and mesh geometry, result in a reasonable fit with the experimental results up to a modest extent over the onset of fracture. For larger separations, however, an accurate model needs to be based on constitutive behaviour obtained from uni-axial tensile testing, as the stress values cannot be determined unambiguously from the CMOD test.

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Disclosure statement

No potential conflict of interest was reported by the authors.

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