Surface Textures for Stretchable Capacitive Strain Sensors

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Abstract.
Advances in smart materials and electronics have enabled compliant sensing systems mimicking nature. In structural health monitoring (SHM) applications, the technology still lacks accuracy in tracking measurements over large geometries. Herein we report a facile fabrication for a compliant sensor based on an elastomeric capacitive sensor technology, augmented with a scalable surface texture inspired by various grid geometries. Texture designs are selected to direct, and therefore improve, the strain sensing capabilities and thus the sensor’s gauge factor. We showcase selected designs based on a detailed engineering analysis. The selected surface patterns are investigated under increasing strain targets and strain frequency sweeps. Results confirm that a stretchable thermoplastic composite sensor with a textured pattern embossed into the hyper-elastic matrix yields approximately 30% increase in the gauge factor and 35% in signal accuracy, great linearity up to 30% strain, and overall signal stability to empower SHM applications.

Keywords: sensor, composite, stretchable electronics, soft elastomeric capacitor, strain monitoring, structural health monitoring, dielectric polymer, texture, sensing skin.

1. Introduction

Advances in soft compliant materials have significantly impacted science and engineering, including the fields of electronics [1, 2], robotics [3, 4], and medicine [5]. Of interest to this paper is the use of soft stretchable materials for structural health monitoring (SHM) applications to design a compliant, dense sensor network with significantly improved adaptability compared with conventional sensors [6]. Such dense
sensor networks, termed sensing skins, aim to mimic biological skin and could provide unprecedented coverage and resolution in terms of state measurement capabilities.

Various sensors fabricated from flexible electronics have been proposed and studied for SHM [7] and other monitoring applications [8, 9, 10]. For instance, flexible resistance-based sensors have been developed using carbon nanotubes [11] and graphene nanosheets [12]. Capacitance-based sensors have been proposed from bio-compatible polymers [13] and nanocomposite thin films [14]. Research examples targeting biological skin mimicry for SHM applications include strain sensing sheets based on large area electronics and integrated circuits [15, 16, 17], electrical impedance tomography [18, 19], and multifunctional materials [20, 21].

The authors have previously studied a sensing skin based on soft elastomeric capacitors (SEC) that transduces strain into a measurable change of capacitance [22]. The choice for a capacitance-based technology was motivated by enhanced mechanical robustness and high scalability, enabling field deployments over large surfaces [23]. The SEC is fabricated from a thermoplastic elastomer mixed with rutile titanium dioxide and sandwiched between conductive composite layers made from the identical elastomer. The sensor technology has been demonstrated in a dense network configuration to measure strain fields on wind turbine blades [23], detect cracks on concrete beam [24], and localize and quantify fatigue cracks on steel components [25]. However, a challenge in the deployment of the SEC technology is in its relatively low gauge factor and its lack of its intrinsic directional sensing capability that requires signal separation to obtain strain measurements along principal axes [26].

The challenge of a directional sensing using soft thin-film sensors is however not new. Recent studies in the field of soft robotics have proposed a woven mesh [27] and rigid compartments [28, 29] acting as strain-limits for more predictable extension, twisting, and bending [30]. Such programmed directionality requires tedious fabrication, which is difficult to integrate into 2D geometries or into any scalable processes for flexible sensors, especially for SHM applications. Addressing the challenges of directional sensing, strain sensitivity, and scalability has the promise to empower thin-film sensor arrays to monitor large structures, such as transportation infrastructures and energy systems, providing rich high-resolution data that could be harnessed for condition-based maintenance decisions.

Previous studies published by the authors have shown that the sensitivity of a un-textured SEC (i.e., no surface corrugation) depends on the dielectric permittivity [31] and can be enhanced on the molecular level, and that its gauge factor is as a function of the transverse Poisson’s ratios of the film and material/structure onto which the sensor is adhered [22] (note that unless otherwise specified, the Poisson’s ratio discussed in this paper refers to the transverse Poisson’s ratio of the SEC). For an un-textured SEC, a lower Poisson’s ratio leads to a higher gauge factor [22]. Adjusting the Poisson’s ratio has been widely employed recently to improve the gauge factor of flexible sensors [32, 33]. Others have proposed the application of wrinkled ultrathin gold films [34] and ionic nanocomposites [35] over the surface of capacitance-based strain sensors to
achieve higher sensitivity. Ameliorating the directionality of measurements of the SEC for SHM has not been addressed previously. In this paper, a surface textured SEC is proposed, with the objective of providing the sensor with higher sensitivity and sensing directionality. This is done by providing axial reinforcement patterns based on various grid geometries. The higher stiffness in the transverse direction leads to lower Poisson’s ratio and minimizes the variation of capacitance induced by the transverse strain.

This paper is organized as follows. Section 2 introduces the design and fabrication process of the textured sensors, and includes a derivation of the electromechanical model. Section 3 describes the methodology used to conduct the numerical study and experimental tests. Section 4 presents and discusses results. Section 5 concludes the paper.

2. Design and Fabrication of Textured Sensors

The fabrication process of an un-textured SEC, described in details in [36], was adapted to fabricate textured SECs as illustrated in Figure 1. To fabricate the textured dielectric layer, styrene-ethylene-butylene-styrene (SEBS) FG1901G (KRATON, USA, $\rho = 1400$ kg/m$^3$, 30%w styrene, permittivity 2.4), and SEBS 500120M (VTC Elastoteknik AB, Sweden, $\rho = 930$ kg/m$^3$, permittivity 2.2) with a weight ratio of 1:3 are dissolved in toluene at 120 g/L. PDMS-coated titania (TPL, Inc., Albuquerque, NM; TiO$_2$($-\text{OSI(CH}_3_3$)$_2$-) particles are added to the stock solution to have a concentration of 12% vol. The particles are added to increase both the durability and permittivity of the dielectric layer [37]. A subsequent sonication employs a dismembrator (high intensity ultrasonic processor Vibracell 75041, Sonics & Materials Inc., USA) for 5 minutes at 120 W and 20 kHz to achieve a stable dispersion of the TiO$_2$. A volume of 20 ml is drop-casted directly after the solicitation onto a 152 mm $\times$ 30 mm non-stick dog-bone steel mold. The molds are made of H13 steel with a HRC48-50 hardness, and their grooves shaped by electrical discharge machining with a maximum depth of 0.35 mm. The mold surfaces have a 1 $\mu$m peak-to-valley accuracy, and an average surface roughness of 0.85 $\mu$m. The drop-casted solution is left to dry over 24 h for the toluene to evaporate. After, the dry composite film is gently removed from the mold and left to dry for another 24 h at room temperature. The resulting film has a thickness of 0.3 mm with a textured height of 0.35 mm and a permittivity of 5.56 computed from capacitance measurements.
An electrode stock solution is prepared by dissolving SEBS 500050M (VTC Elastoteknik AB, Sweden) in toluene at 380 g/L and adding 15% vol of carbon black (CB) particles (Orion, Kingwood, TX). For a homogenous dispersion, a low-speed homogenizer is used for 1 hour at 650 RPM. The top and bottom electrodes are brushed onto the dielectric layer with a consecutive drying step for four hours to create uniform conductive layers with a sheet resistance of approximately 2.6 kΩ/Sq (Noncontact Eddy Current Sheet Resistance Meter, 20J3, Delcom Instruments, Prescott, WI) and mean thickness of 25 µm. Adhesive copper tapes and PELCO conductive carbon glue (TED Pella, INC., USA) are installed over each side of the sensor, and used to provide mechanical and electrical connections to the data acquisition system (DAQ) for charging and discharging the capacitor. Figure 2 schematizes materials’ deformation behaviors for an un-textured and a textured SEC under strain.
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2.1. Electromechanical Model

At low measurement frequency (< 1 kHz), the SEC is modeled as a non-lossy plate capacitor:

$$ C = \varepsilon_0 \varepsilon_r \frac{A}{h} $$

where $\varepsilon_0 = 8.85 \text{ pF/m}$ is the vacuum permittivity, $\varepsilon_r$ is relative permittivity of the dielectric, $h$ is the thickness of the dielectric, and $A = w \cdot l$ is the electrode area as annotated in Figure 2(a). Assuming small changes in axial strain, differentiating Eq. 1 yields an expression relating strain to the relative change in capacitance $\Delta C/C$ with the in-plane strains $\varepsilon_x = \Delta l/l$ and $\varepsilon_y = \Delta w/w$, and out-of-plane strain $\varepsilon_z = \Delta h/h$.

$$ \frac{\Delta C}{C} = \left( \frac{\Delta l}{l} + \frac{\Delta w}{w} - \frac{\Delta h}{h} \right) $$

Taking the SEC as deployed onto a surface along the $x - y$ axes and assuming no pressure along the $z$ axis, one can apply Hooke’s law under plane stress assumption with

$$ \varepsilon_z = -\frac{\nu}{1-\nu} (\varepsilon_x + \varepsilon_y) $$

Similar to the polymer–nanoparticle hybrid dielectrics [38], this flexible dielectric is created by incorporating nanoparticles of high dielectric permittivity within the polymer, and the un-textured SEC film is isotropic in all directions characterized by...
the Poisson’s ratio $\nu$. Substituting the expression for $\varepsilon_z$ from Eq. 3 into Eq. 2 provides the electromechanical model for the un-textured SEC.

$$\frac{\Delta C}{C} = \frac{1}{1 - \nu} (\varepsilon_x + \varepsilon_y)$$  \hspace{1cm} (4)

Textured patterns have stiffness diversity and inhomogeneity of tensile-induced stress distribution on the dielectric layer. Thus, the dielectric layer is treated as orthotropic in the $x$-$y$ plane. The properties along the sensor differ significantly from those across the sensor governed by the Poisson’s ratio $\nu_{xy}$:

$$\nu_{xy} = -\frac{\varepsilon_y}{\varepsilon_x}$$  \hspace{1cm} (5)

Substituting Eq. 5 into Eq. 4 yields an electromechanical model for the textured SEC:

$$\frac{\Delta C}{C} = \frac{1 - \nu_{xy}}{1 - \nu} \varepsilon_x$$  \hspace{1cm} (6)

where $\nu$ is here defined as $\nu = \nu_{xz} = \nu_{yz}$, corresponding to the Poisson’s ratio of the un-textured SEC. It follows that the gauge factor $\lambda$ can be expressed as

$$\lambda = \frac{1 - \nu_{xy}}{1 - \nu}$$  \hspace{1cm} (7)

Eq. 7 shows that the capacitance gauge factor solely varies as a function of the Poisson’s ratio, which is expected for polymers under axial strain at constant temperature [39]. Assuming $\nu$ and $\nu_{xy}$ decrease proportionally with increasing strain, the gauge factor $\lambda$ tends to a near-constant value. This will be experimentally verified in Section 4. Remark that the gauge factor of the sensor will be also influenced by its installation, as derived in [22] for an un-textured SEC adhered onto structural materials. This paper limits the investigation to a freestanding configuration to allow a direct study of the effects of the texture, while structural health monitoring applications are left to future work.

2.2. Surface Patterns

The surface patterns are created from embossed geometries forming lattices. Geometric discontinuities and varieties are designed to cause inhomogeneous stress distributions, thus altering the Poisson’s ratio. Figure 3 shows the selected mesh types and layouts. The patterns forming the textures are selected to increase the transverse stiffness of the materials, therefore decreasing $\nu_{xy}$. Pattern designs A and C follow a diagrid-like pattern with intersecting diagonal reinforcement [40], where Pattern A includes vertical reinforcements. The diagonal elements are angled at 36°, consistent with the optimal angle for a diagrid system in terms of transverse stiffness [41]. Patterns B, E, and F have grid-like arrangements consisting of evenly spaced horizontal and vertical straight lines. Pattern F has fewer vertical reinforcements, but more horizontal reinforcements.
Patterns B and E have the same grid arrangement, but with Pattern B constructed using thicker strips. Pattern D is a grid pattern with curved vertical reinforcement, inspired by the design of metal interconnects in stretchable electronics [42, 43] to provide a lateral geometric constraint for tensile-induced transverse shrinkage [44]. Pattern G is a un-textured SEC taken for benchmarking results. Note that the sequence of patterns is organized following the results from the numerical simulations (presented below), from the lowest to the highest resulting Poisson’s ratio $\nu_{xy}$.

![Figure 3. Schematics of (a) Pattern A; (b) Pattern B; (c) Pattern C; (d) Pattern D; (e) Pattern E; (f) Pattern F; and (g) Pattern G.](image-url)
3. Methodology

3.1. Numerical Model

Quasi-static nonlinear finite element (FE) simulations were conducted in ANSYS 2019 R2 to evaluate the performance of the selected patterns. Three-dimensional FE models were created by importing the AutoCAD drawings (Figure 3) into ANSYS, and material properties were assigned as isotropic with a stiffness of 0.41 MPa experimentally obtained from quasi-static tests on un-textured sensors.

Digital image correlation (DIC) [45] was used in this study to experimentally measure the Poisson’s ratio $\nu$ of the dielectric film. A 52.3 × 6.92 × 0.32 mm$^3$ non-textured 5:1 aspect ratio dielectric film was subjected to uniaxial strain by utilizing a Discovery Hybrid Rheometer (DHR-2, TA Instruments). The dimensions of the specimen, in the coupled uniaxial strain and DIC experiments, were selected to meet the requirements of the thin-film tension clamps attached to the DHR-2 device. The film was stretched to 30% strain at a linear rate of 80 $\mu$m/s for a period of 130 s. A preload of 0.01 N was applied prior to each experiment to eliminate sample buckling. During the uniaxial strain experiments, surface deformations were simultaneously obtained by applying DIC method [46]. For that purpose, two digital cameras (FASTCAM SA-Z, Photron) were calibrated for stereovision and used to image un-textured specimens during the stretching process at a frame rate of 60 fps. The DIC software VIC-3D (Correlated Solutions, Inc., Columbia, SC, USA) was used for data extraction and analysis. The specimens were speckled with black spray paint, which allowed the DIC algorithm to be applied for the extraction of the transverse strains that are needed to compute the transverse Poisson’s ratio. Figure 4(a) shows the distribution of the digitally obtained Poisson’s ratio of this dielectric film under 10% strain. A region of dimensions 7 × 7 mm$^2$ around the transverse centerline (identified by the red box in the figure) was defined as the region of interest (ROI) for extracting the Poisson’s ratio data used in the analysis. The 7 mm length corresponds to a 20% gauge length, selected to achieve an ROI with dimensions comparable with the sample’s width. Within the ROI, there were no changes in the Poisson’s ratio values when rounding to the nearest 100th (under each strain level), which implies that the boundary condition effects attributed to the clamped ends were negligible. The Poisson’s ratio value $\nu$ under each strain level $\varepsilon$ was taken as the ROI area-wide average value. Results, plotted in Figure 4(b), were fitted using a 4-degree polynomial ($R^2 = 0.9952$) and the function added to the numerical model to define the Poisson’s ratio strain-dependent behavior.

Automatic triangular meshes with an element size of 0.2 mm (maximum size for mesh convergence in this simulation) were generated using the tetrahedral method. Boundary conditions were assigned as constrained fixed along the $x$ and $y$ translational degrees-of-freedom (UX and UY) at the left-hand-side, and constrained fixed along the $y$ translational degree-of-freedom (UY only) at the right-hand-side, as illustrated in Figure 9(b). Each pattern was simulated by applying 5% and 30% axial strain along the right-hand-side support with loading rates of 0.01 mm/s and 0.3 mm/s. The
ROI used to extract the numerical model’s data was consistent with that of the DIC experiment. Synthetic measurements were taken over 12 nodes along the edges of the sensor, indicated in Figure 9(b), to compute the average strain response in both longitudinal \((x)\) and transverse directions \((y)\).

![Poisson's ratio distribution from DIC measurements](image)

**Figure 4.** (a) Poisson’s ratio distribution from DIC measurements, showing an overlaid \(7 \times 7 \text{mm}^2\) ROI (red box); and (b) transverse Poisson’s ratio for un-textured film under increasing strain, obtained by averaging DIC measurements within the ROI under each strain level.

### 3.2. Experimental Tests

The specimens (including the un-textured specimen) were dog bone-shaped specimens with a total length of 160 mm, gauge length of 111.2 mm, and width of 32 mm (Figure 3). The strain experiments were conducted using an Instron 5969 dual column tabletop with \(\pm 0.01\) mm or 0.05\% of displacement measurement accuracy equipped with a 2580 series load cell (shown in Figure 5). The passive non-electrode ends of the dog bone specimens were clamped between fiberglass plates adhered to the specimens with an epoxy (JB Weld) and mounted into the tensile tester. Displacements converted to longitudinal strain and axial forces were recorded using a Bluehill DAQ with a 12 Hz sampling frequency.

Two specimens were tested under each pattern. Before each test, each specimen was initially pre-strained by 0.5\% at a constant loading rate of 0.1 mm/s. For the quasi-static tests, target strain was applied at a constant rate and then released. This procedure was repeated three times for each sample. For dynamic tests, specimens were subjected to harmonic excitations of varying frequencies under a constant strain amplitude level, and to triangular excitations of varying strain amplitude levels under a constant frequency. Capacitance measurements were recorded using a LCR meter (Agilent 4263B) with a testing frequency of 1 kHz and sampling frequency of 10 Hz. Signal accuracy through capacitance-strain signal matching and the gauge factor were used as metrics to evaluate the performance of the sensors. Note that DIC was not utilized to obtain the transverse Poisson’s ratio \(\nu_{xy}\) of textured films directly; instead, it was obtained numerically from the models.
4. Results and Discussion

4.1. Numerical Model Validation

The validation of the numerical model was conducted by matching force-strain curves obtained experimentally on the seven patterns. Figure 6 compares the experimental and numerical axial forces versus strain over 30% strain under each pattern, where the experimental strain values represent the average of two specimens and numerical values were obtained by simulating the same axial forces (Figure 6). The relative displacement of each nodes along the $x$-direction are extracted and averaged to calculate the longitudinal strain under axial forces. The root mean square errors (RMSE) of the fit are listed in the top left of Figure 6. The overall RMSE values range from 0.65 to 1.19% strain, which indicates a good match between the experimental data and numerical models, therefore validating the accuracy of the numerical model. Discrepancies in results can be attributed to the hand-fabrication process. Results from Figure 6 can also be used to study the axial stiffnesses of each pattern. Compared to the un-textured SEC (Pattern G), all of the textured sensors exhibit a higher axial stiffness, confirming the effect of axial reinforcements provided by the textured surfaces.
4.2. Numerical Study

The validated numerical model was used to investigate the effects of different texture geometries on stress distribution. Figure 7 presents the principal normal stress distribution obtained numerically for each pattern under 10% longitudinal strain. It can be observed that 1) the overall tensile-induced stresses (positive value) are distributed on the substrate films and longitudinal strips; 2) the compression stresses (negative value) are concentrated in the raised strips along the transverse direction due to the inhomogeneity of the textured configuration; 3) the existence of transverse (or vertical) strips significantly reduces the magnitude of tensile-induced stresses distributed in the substrate layer; and 4) a crescent-shaped compression stress formed at the right-hand-side of each dog bone specimen, attributable to the asymmetric boundary conditions.
The relative displacements between each node along the $y$-direction are extracted and averaged to calculate the transverse strain under axial stretch. After, the transverse Poisson’s ratio $\nu_{xy}$ was obtained numerically from the resulting transverse shrinkage under 1%, 5%, 10%, 20%, and 30% longitudinal strain. Figure 8 plots the results. The transverse Poisson’s ratios increase from Pattern A to G under all strain levels. It follows that Pattern A is expected to have the highest strain sensitivity and Pattern G the lowest.
Results also reveal that the stress in the transverse direction is tunable by altering the width and height of the strips to provide additional cross-sectional area, a process known as mechanical stiffening. It is noticed that Patterns B and E, which have the same grid geometry but different strip width, yield two different transverse Poisson’s ratios, whereas the use of a larger cross-sectional area results in a lower Poisson’s ratio. An enhanced investigation on the effect of strip widths and heights on the transverse Poisson’s ratio was conducted using Pattern A. A first set of simulations held the strip width fixed at 1.4 mm, and varied the strip height from 0 to 0.9 mm in 0.05 mm intervals. Results, plotted in Figure 9 (black line), indicate that the transverse Poisson’s ratio decreases as the strip height increases and converges at approximately 0.8 mm, with a notable gain on $\nu_{xy}$ between 0.2 mm and 0.5 mm. A second set of simulations held the strip height constant, but varies the strip width from 0 to 1.8 mm in 0.1 mm intervals. Results, plotted in Figure 9 (red line), also shows convergence of the transverse Poisson’s ratio, here at approximately 1.5 mm, and an important transition zone, here between 0.4 mm and 1.2 mm. Although results from this investigation conclude on 0.8 mm being an optimal height, a height of 0.35 mm was selected to ease the fabrication process (from the manual demolding), with a strip width of 1.4 mm.
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Figure 9. (a) Averaged transverse Poisson’s ratio of Pattern A under varying strip height and width; and (b) schematic of the numerical model showing the geometry, boundary conditions, nodes used for extraction of synthetic measurements, and loading direction.

4.3. Quasi-Static Tests

Quasi-static tests were conducted to investigate the linearity of the sensor under high levels of strain (up to 30%) applied at a rate of 0.3 mm/s. Figure 10(a) plots the relative change in capacitance $\Delta C/C_0$ versus the applied strain, with the black dashed lines resulting from a linear fit of the experimental data. The overlap of experimental data by the solid lines illustrates the high quality of the linearity of the sensor. This linearity obtained up to 30% demonstrates a net advantage of the sensor over conventional off-the-shelf strain gauges.

Figure 10. (a) Quasi-static tensile test results over 30% strain under each pattern; and (b) signal matching error for Pattern A with 95% confidence intervals.

Experimentally obtained gauge factors ($\lambda_{\text{exp}}$) were taken directly from the fitted experimental data (Figure 10(a)), and compared against the numerically obtained gauge factors ($\lambda_{\text{num}}$) taken by substituting the numerically obtained transverse Poisson’s ratio...
into Eq. 7. Additional tests were performed at a lower strain rate, 0.01 mm/s, applied up to 5%, to evaluate the stability of the gauge factor. Results under each pattern are tabulated in Table 1. It can be observed that the experimental gauge factors are approximately 2% to 9% higher than the numerically predicted values, with the discrepancy being generally higher for textures yielding higher gauge factors. These discrepancies may be caused by the manual fabrication process causing unmodeled stress concentrations, in particular for textures of higher complexities such as Patterns A and C. Overall, the ranking of the patterns in terms of gauge factors (or transverse Poisson’s ratio) remains the same, with Pattern A exhibiting an improvement of approximately 30% in the gauge factor with respect to Pattern G (un-textured SEC). Comparing results for different loading rates shows that the gauge factor is stable, with a maximum variation of 0.3% found in Pattern G.

<table>
<thead>
<tr>
<th>Pattern</th>
<th>0.01 mm/s</th>
<th>0.3 mm/s</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\lambda_{\text{exp}}$</td>
<td>$\lambda_{\text{num}}$</td>
</tr>
<tr>
<td>A</td>
<td>1.268</td>
<td>1.163</td>
</tr>
<tr>
<td>B</td>
<td>1.231</td>
<td>1.148</td>
</tr>
<tr>
<td>C</td>
<td>1.220</td>
<td>1.131</td>
</tr>
<tr>
<td>D</td>
<td>1.181</td>
<td>1.122</td>
</tr>
<tr>
<td>E</td>
<td>1.169</td>
<td>1.106</td>
</tr>
<tr>
<td>F</td>
<td>1.116</td>
<td>1.080</td>
</tr>
<tr>
<td>G</td>
<td>0.976</td>
<td>1.000</td>
</tr>
</tbody>
</table>

Next, the signal matching error over small strain levels was investigated under a strain rate of 0.01 mm/s applied up to 5% strain. Figure 10(b) plots the result under Pattern A, selected because it exhibited both the highest experimental and numerical gauge factors, with results zoomed over the 0-0.20% strain region for clarity. The plot also shows the linear fit and 95% confidence interval (CI) bound $\pm 0.0293 \Delta C / C_0 \, (%)$, equivalent to $\pm 2.29 \mu \varepsilon$ using the electromechanical model (Eq. 7) with the gauge factor $\lambda = 1.268$. Comparatively, the 95% CI bound for the un-textured SEC (not shown in the plot) is $\pm 0.0345 \Delta C / C_0 \, (%)$, equivalent to $\pm 3.53 \mu \varepsilon$ (Eq. 7 with $\lambda=0.976$). Pattern A resulted in a 35% improvement in accuracy.

The effects of the loading rate was further studied for Pattern A using the consequent strain rates of 0.0005 mm/s, 0.001 mm/s, 0.003 mm/s, 0.005 mm/s, 0.01 mm/s, 0.03 mm/s, and 0.3 mm/s to achieve a strain target of 2.5%. Table 2 lists $\lambda_{\text{exp}}$ along with the 95% CI bound and equivalent strain accuracy using Eq. 7 and the associated $\lambda_{\text{exp}}$. It is evident that the gauge factors are constant for different loading rates, with a maximum difference of 1.04%. However the accuracy of gauge factor decreases with increasing loading rate, which is attributed to the overall mechanical
properties of the employed soft composite and the performance of the load cell at low
strain rates.

<table>
<thead>
<tr>
<th>Loading rate (mm/s)</th>
<th>( \lambda_{\text{exp}} )</th>
<th>( \Delta C/C_0 ) (%)</th>
<th>( \mu \varepsilon )</th>
</tr>
</thead>
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<tr>
<td>0.0005</td>
<td>1.259</td>
<td>±0.0341</td>
<td>±2.708</td>
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<tr>
<td>0.001</td>
<td>1.256</td>
<td>±0.0334</td>
<td>±2.659</td>
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<tr>
<td>0.003</td>
<td>1.268</td>
<td>±0.0325</td>
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<td>0.005</td>
<td>1.269</td>
<td>±0.0306</td>
<td>±2.411</td>
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<td>±2.363</td>
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<td>±0.0272</td>
<td>±2.155</td>
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<td>0.3</td>
<td>1.261</td>
<td>±0.0193</td>
<td>±1.531</td>
</tr>
</tbody>
</table>

4.4. Dynamic Tests

Pattern A was selected to conduct tests under dynamic loads because it exhibited the best performance in terms of gauge factor \( \lambda \). Tests consisted of applying a harmonic load of 1.11 mm amplitude (1% strain) of increasing frequency at 0.1, 0.3, 0.5, 0.8, 1.1, and 1.3 Hz, with each of the frequency applied over 10 cycles. Figure 11(a) presents a time series plot for Pattern A, compared against the strain input. It is observed that the sensor is capable of tracking the strain, but with an increasing error with an increasing frequency input, in particular beyond 1 Hz. That phenomenon is possibly due to the strain-rate dependency of the SEBS material and the softer bonds caused by the adiabatic heating effect, as discussed in a previous study on the dynamic characterization of the SEC technology [47].

Figure 11(b) is a plot of the response in the Fourier domain for Pattern A, showing that the frequency response of the sensor can track the frequency content of the input. Figure 11(b) also presents the extracted gauge factors as a function of the frequency input, representing the frequency response function, for each sensor pattern under investigation. Results show that the gauge factor decreases with increasing frequency, consistent with the findings in previous work on the SEC [48], where the dynamic behavior of the gauge factor was modeled up to 40 Hz. The ranking of sensor performance in terms of \( \lambda \) remains consistent throughout each frequency input.
Lastly, the stability of the gauge factors under dynamic loads was evaluated by subjecting the specimens to triangular loads at a constant frequency of 0.3 Hz of increasing strain amplitude at 0.5, 1, 1.5, 2, 3, 4, and 5%, with each strain level applied over 5 cycles. Figure 12(a) plots the time series response of Pattern A against the strain input. Results show good agreement, with the error slightly increasing with the increasing strain levels, which may be an intrinsic contribution of the materials composite itself. The experimental gauge factor under each pattern and applied strain amplitude is plotted in Figure 12(b). It is worth to notice that the gauge factors remain approximately constant throughout each strain level.
This paper proposed to texture stretchable capacitive-based strain sensors in order to improve the directionality of measurements. Textures are created by drop-casting an SEBS-composite solution in molds equipped with surface patterns. Six textures were proposed and their performance assessed in terms of reducing the transverse Poisson’s ratio, and thus augmenting the gauge factor over uniaxial strain. This was done using both numerically through a validated finite element model, and experimentally by characterizing the electrical signal as a function of quasi-static and dynamic strain inputs. The performance was assessed against that of an un-textured sensor, i.e. without texture.

Results from the numerical investigation showed that altering the lattice’s geometry, strip heights, and strip widths had a significant effect on the transverse Poisson’s ratio. The optimal design was identified as a diagrid with transverse reinforcement vertical strips (Pattern A). Experimental tests confirmed that such geometry yielded an increase of 30% in the gauge factor compared to the un-textured sensor, improvement in signal accuracy of approximately 35%, and maintained linearity of the sensor signal up to 30% strain. The gauge factor showed nearly constant as a function of strain rate and
maximum applied amplitudes, but decreased over increasing loading frequencies. While
this frequency-dependence of the loading rate was not modeled, it was consistent with
results from previous work on the un-textured sensor.

Overall, the results presented in this paper demonstrated the practicability of
textured stretchable capacitive strain sensors with potential applications for structural
health monitoring. The application of these stretchable sensors in dense networks has the
promise to improve strain mapping capabilities for condition assessment applications.
It is foreseen that texturing of sensors will become an essential feature of bio-inspired
sensing skins due to the ease of fabrication and improve sensing performance, with
potential deployments in other fields such as bio-sensing and soft robotics.

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