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Design and prototyping of self-centering optical single-mode fiber alignment structures

Evert Ebraert¹,³, Fei Gao¹,³, Stefano Beri², Jan Watta², Hugo Thienpont¹,³ and Jürgen Van Erps¹,³

¹ Brussels Photonics Team (B-PHOT), Department of Applied Physics and Photonics, Vrije Universiteit Brussel (VUB), Pleinlaan 2, B-1050 Brussels, Belgium
² Commscope, R&D Optics Advanced Engineering, Diestsesteenweg 692, 3010 Kessel-Lo, Belgium

E-mail: eebraert@b-phot.org

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Abstract

The European Commission’s goal of providing each European household with at least a 30Mb s⁻¹ Internet connection by 2020 would be facilitated by a widespread deployment of fibre-to-the-home, which would in turn be sped up by the development of connector essential components, such as high-precision alignment features. Currently, the performance of state-of-the-art physical contact optical fiber connectors is limited by the tolerance on the cladding of standard telecom-grade single-mode fiber (SMF), which is typically smaller than ±1 μm. We propose to overcome this limit by developing micro-spring-based self-centering alignment structures (SCAS) for SMF-connectors. We design these alignment structures with robustness and low-cost replication in mind, allowing for large-scale deployment. Both theoretical and finite element analysis (FEA) models are used to determine the optimal dimensions of the beams of which the micro-springs of the SCAS are comprised. Two topologies of the SCAS, consisting of three and four micro-springs respectively, are investigated for two materials: polysulfone (PSU) and polyetherimide (PEI). These materials hold great potential for high-performance fiber connectors while being compatible with low-cost production and with the harsh environmental operation conditions of those connectors. The theory and FEA agree well (<3% difference) for a simple micro-spring. When including a pedestal on the micro-spring (to bring it further away from the fiber) and for shorter spring lengths the agreement worsens. This is due to spring compression effects not being taken into account in our theoretical model. Prototypes are successfully fabricated using deep proton writing and subsequently characterized. The controlled insertion of an SMF in the SCAS is investigated and we determine that a force of 0.11 N is required. The fiber insertion also causes an out-of-plane deformation of the micro-springs in the SCAS of about 7 μm, which is no problem for robustness according to the FEA model. Finally connector-assemblies are made with the alignment system and we show that an insertion loss down to 0.1 dB is achievable.

Keywords: fiber connector, finite element analysis, optical fiber, optical telecom, alignment structures, deep proton writing, fiber-to-the-home

(Some figures may appear in colour only in the online journal)
1. Introduction

Cloud-based services, high-definition video on demand and other such applications demand a vast amount of bandwidth, not only on the global network level (the so-called ‘backbone’), but also on the consumer side (the so-called ‘last mile’). The European Commission, recognising this ever-increasing demand for bandwidth, set as a goal for its telecommunication infrastructure to provide each household in the European Union with at least a 30 Mb s\(^{-1}\) bitrate connection by the year 2020 [1]. Ideally fiber-to-the-home (FTTH) networks should be deployed, since they provide a future-proof solution in the sense that these networks could easily be upgraded beyond the required 30 Mb s\(^{-1}\) in the future [2]. Even though FTTH and FTTB (fibre-to-the-building) penetration rose by 50% from 2014 to 2015 in Europe, the growth rate has dropped to 19% for the first nine months of 2015 [3, 4]. The biggest drawback of deploying FTTH networks remains their high deployment cost. A large contributor to the deployment cost of an FTTH network is the huge amount of single-mode fiber (SMF) connections that are needed [5]. Requiring a lot of SMF connections results in both an increased capital (CAPEX) and operational expenditure (OPEX). Up until now, mostly fusion splices and factory-terminated patch cords with ferrule-based connectors are being used [5]. In the past this was a viable approach since connections were mostly made in central offices during deployment of the backbone of the network. When aiming to deploy FTTH networks, fusion splicing has serious drawbacks. Specialized and expensive equipment needs to be used and splices are permanent connections, which cannot be plugged or unplugged [5]. In an effort to reduce the deployment cost and thus further increasing FTTH penetration, lower-cost and/or more performant SMF connection techniques are needed.

Considering mechanical splices, i.e. physical contact connections, as a cheaper alternative to fusion splicing, there is a fundamental limitation on the performance due to the use of ferrules or V-grooves for alignment [6, 7]. This type of alignment structures uses the outer diameter of the bare silica SMF (without protective coatings) as a reference for alignment and typically achieves insertion losses of 0.2 dB [8]. The performance of mechanical splices is hence limited by the fabrication tolerance on the cladding diameter of an SMF, at this time. This fabrication tolerance is maximally ±1 μm according to the G.652 recommendation of the Telecommunication Standardization Sector of the International Telecommunication Union (ITU-T) [9] and to the IEC 60793-2-50 standard of the International Electrotechnical Commission (IEC) [10]. In practice it is typically of the order of ±0.7 μm for commercially available SMF [11]. Our simulations (using fully vectorial mode solving) show that a lateral misalignment of 0.7 μm leads to an insertion loss of 0.05 dB [8]. In this paper we therefore propose a self-centering alignment structure (SCAS) in which three or four micro-springs are used to center an SMF in the alignment structure, as shown in figures 1(b) and (c). Self-centering optical fiber holders created by combining proton beam writing and symmetric swelling of polymethylmethacrylate (PMMA) have been reported [12]. This approach requires the diffusion of a monomer while a fiber is inserted into the irradiated and etched micro-holes, rendering it impractical for large-scale deployment. Micro-spring-based self-centering structures based on existing patents [13, 14] have been fabricated by Holm et al using deep reactive ion etching in silicon [15]. Since these structures were fabricated to prove the capabilities of the fabrication technology, no additional data their design or performance is available. The self-centering alignment features we propose, can be realized by creating a structure with deformable segments, which act as springs, as shown in figures 2(b) and (c). A central opening for inserting a bare G.652 telecom fiber is foreseen in-between the deformable segments. This opening should be smaller than the nominal 125 μm diameter of a bare SMF, such that as a bare SMF is being inserted, the springs deflect and in doing so hold the fiber into place while centering it. Notice that the SCAS only centers the fiber in lateral direction. To obtain a good angular alignment, we include a pre-alignment plate in the connector assembly (see section 4). To ensure a good physical contact between the two mating fibers in a connection, we envisage using a controlled buckle of the fiber in each connector to provide an axial force [16]. A connector-assembly made with the SCAS is called a self-centering connector (SCC) for the rest of the paper. In section 2 we design the SCAS with a theoretical and finite element approach, with low-cost replication already in mind. In order to align two of the proposed SCASs, mechanical transfer (MT) pin-holes are included in the design, which allows for compatibility with commercial multiple push-on (MPO) connectors [17] as defined in the fiber optic connector intermateability standard (FOCIS) TIA-604-5 or with mechanical transfer registered Jack (MT-RJ) connectors [18] as defined in FOCIS TIA-604-12. Using two metal MT-pins, a high-efficiency mechanical splice between two SMFs can be achieved with the SCCs. When making a connection between an SCC and a commercially available MPO connector, the connection is established with only one of the twelve fibers in the MPO connector, since at this stage we design our SCAS for only a single fiber in our SCC. In section 3, we discuss the prototyping of the designed structures and their geometrical characterization. In section 4 we assemble our SCAS into an SCC and we investigate the fiber insertion in section 5. The insertion loss obtained with our SCCs is measured in section 6. Finally, to enable mass deployment of the proposed SCAS, it needs to be manufacturable in large volumes at low cost, and it needs to be compatible with stringent environmental operation conditions of optical fiber connectors. In section 7 we therefore discuss the transition towards replication of the SCAS in high-performance thermoplastic polymers.

2. Design of self-centering alignment structures

2.1. Vibration endurance

Our SCAS should be compliant with the IEC standard on vibration endurance [19] while not allowing the fiber’s lateral misalignment to exceed 0.29 μm, which corresponds to
an insertion loss of 0.01 dB according to our simulations. This insertion loss is calculated by calculating the gaussian beam overlap and by assuming a fiber core radius of 4.15 μm [8] and a difference in refractive index between core and cladding of 0.2%. Consider now the two-spring system depicted in figure 1(a), which can be described by

\[ \omega_0 = \sqrt{\frac{2k_1}{m}} = \sqrt{\frac{k_{\text{eff}}}{m}}. \tag{2} \]

Here \( k_1 \) is the spring constant of an individual spring and \( k_{\text{eff}} \) the effective spring constant for the system. The transfer function \( h(s) \) of this system is obtained after a Laplace transform of (1)

\[ ms^2x(s) + k_{\text{eff}}x(s) = k_{\text{eff}}x_0(s) \tag{3} \]

\[ h(s) = \left( \frac{s^2}{\omega_0^2} + 1 \right)^{-1}. \tag{4} \]

Since we are interested in limiting the movement of the SMF (the mass) in this system, we need an expression for its displacement \( \Delta x \).

\[ \Delta x = x(s) - x_0(s) = \frac{-s^2}{s^2 + \omega_0^2}x_0(s). \tag{5} \]

Consider now a 5g excitation of the mass in this system, in accordance with the aforementioned vibration endurance standard. We can then substitute the amplitude of the vibration, \( -s^2x_0(s) \) by 5g, after which we can express the displacement of the mass as

\[ \Delta x = \frac{5g}{\omega^2 + \omega_0^2}. \tag{6} \]

Substituting (2) in (6) we gain an expression for the minimal spring constant this system should have in order to limit the displacement of the mass to \( \Delta x \) [20].

\[ k_{\text{eff}} \geq m\left( \frac{5g}{\Delta x} - \omega^2 \right) \tag{7} \]

Where \( k_{\text{eff}} = 2k_1 \) for the two-spring (figure 1(a)) or four-spring (figure 1(c)) system and \( k_{\text{eff}} = \left(\frac{3}{2}k_1 \right) \) for the three-spring system as depicted in figure 1(b). If we now fill in the maximal displacement value of 0.29 μm corresponding to an insertion loss of 0.01 dB as described earlier), estimate the mass of 1 cm of SMF as 2.7 × 10⁻⁷ kg (using a density of 2201 kg m⁻³ for the SMF [21]) and the maximal mechanical vibration frequency of 5 kHz, we obtain a minimum value of 38.9 N m⁻¹ for the effective spring constant. Note that this results in a minimal value of 34.9 N m⁻¹ for each individual spring in the three-spring system and a value of 26.2 N m⁻¹ for each individual spring in the four-spring SCAS to satisfy the vibration endurance standard.  

2.2. Theoretical approach

To practically implement the springs of the self-centering alignment systems proposed in figure 1, we choose to design deflectable micro-beams. These can be modeled as a beam clamped at both ends, as depicted in figure 3(a). The SMF touches the beam in a single point and can thus be approximated by a point-load \( P \), acting on the contact point. This is a statically indeterminate beam, for which we have six unknowns and only three static equations. In other words the beam is indeterminate to the third degree. Consequently, three additional equations are necessary for the solution to be found. To obtain these equations, the principle of superposition is used and the compatibility of the displacement at the support is considered. We apply the Müller–Bresslau theorem (also known as the force method) [22] and we assume the three reaction forces of the right-hand side of the beam to be redundant and assume that the clamped boundary condition
does not apply, as shown in figure 3(b). Using the principle of superposition, the set of three ‘redundant systems’, where each redundant reaction force is unity while the others are zero, can be used to determine the flexibility matrix $\delta_{ij}$ and compile the compatibility equation given in (8) [22]:

$$
\begin{bmatrix}
\delta_{11} & \delta_{12} \\
\delta_{21} & \delta_{22}
\end{bmatrix}
\begin{bmatrix}
U_1 \\
U_2
\end{bmatrix}
+ \begin{bmatrix}
\delta_{1p} \\
\delta_{2p}
\end{bmatrix} = \begin{bmatrix}
C_1 \\
C_2
\end{bmatrix}
$$

where $C_i$ are the displacements of the redundant point (right-hand side) of the beam due to the force $P$ in the statically indeterminate system, thus zero in this case. $\delta_{ij}$ are the displacements in the redundant systems due to the force $P$ only and $U_i$ are the redundant reaction forces. Note that $U_3$ is not included here, since there is no effect of the horizontal forces on the vertical reaction force and the reaction moment. The elements of the flexibility matrix $\delta_{ij}$ can be calculated using (9):

$$
\delta_{ij} = \int_0^L \frac{M_M}{EI} dx
$$

where $M_M$ is the moment distribution as a function of the position $x$ along the beam (see figure 3(a)) over the integration length due to a single redundant force $U_i$ or the force $P$. $E$ is the Young’s modulus of the material out of which the spring will be made and $I$ is the moment of inertia. Solving (8) allows us to determine that $U_1 = P/2$ and that $U_2 = PL/8$. Because of the symmetry of the system we know that $V_1 = U_1$ and $M_1 = U_2$. With the reaction forces known, we need to calculate the displacement of the beam given a unity load acting in the center of the spring to determine its spring constant. Pasternak’s theorem states that the unity load method can still be used in case the system is statically indeterminate, which allows us to calculate the displacement $\delta$ using (9), if instead of $M_M$ we use the moment of the statically indeterminate structure and for $M$ the moment of the statically determinate system with a unity load acting in the center of the beam.

$$
\delta = \int_0^{L/2} \frac{L}{8} \left(1 - \frac{x}{L} \right) \left(\frac{L}{2} - x \right) \frac{E}{I} dx
+ \int_{L/2}^L \frac{L}{8} \left(1 - \frac{x}{L} \right) 0 \frac{E}{I} dx = \frac{L^3}{192EI}.
$$

The spring constant is the inverse of the displacement of the beam due to a unity force acting in its center. Filling in the expression for the moment of inertia $I$, allows us to write the spring constant of the beam clamped at both ends as a function of its thickness ($T$), width ($W$), length ($L$) and Young’s modulus.

$$
k = \frac{16ETW^3}{L^3}.
$$

With mechanical robustness in mind, we set a lower limit of 100 $\mu$m to the width ($W$) of the micro-spring beam. Since designing the springs with too high a spring constant would result in difficulties to insert a bare G.652 SMF in the SCAS, we choose this lower limit as a target value for the width. Considering now the compatibility of this design with replication technologies, we consider an upper limit on the aspect ratio of 1 : 5 [23], which means that there is an upper limit on the spring thickness ($T$) of 500 $\mu$m. To maximize the robustness of the design we choose this upper limit as a target value for the thickness of the spring. The material of choice for these SCASs is polysulfone (PSU) or polyetherimide (PEI), since these polymers are commonly used thermoplastic materials for micro hot embossing and micro-injection moulding [24–28], have a high creep resistance and are compatible with the environmental conditions to which fiber connectors have to comply (temperatures ranging from $-40$ °C to $70$ °C) [29–31]. PSU and PEI have a Young’s modulus of respectively $2.6 \times 10^9$ Pa and $3.3 \times 10^9$ Pa. Knowing all but one of the variables in (7) allows us to determine the maximum spring length ($L$) allowed to obtain a spring constant which still satisfies the vibration endurance criterion discussed in section 2.1. All values mentioned in the rest of the paper are those obtained for PEI unless mentioned otherwise (the results for PSU are similar since its material parameters are in the same order of magnitude as PEI). The maximal spring length then becomes 9.1 mm for the three-spring and 10.0 mm for the four-spring SCAS. These lengths are of course disproportionally large compared to the fiber diameter of 125 $\mu$m. Indeed if we consider that the three springs forming the three-spring SCAS depicted in figure 1(b) would be springs as depicted in figure 3(a), there would inevitably be a partial overlap between the three springs unless a spring length ($L$) smaller than about 110 $\mu$m would be considered. However, since short springs would yield too much of a problem for fiber insertion due to their excessively high spring constant it is not physically possible to realize a three or four-spring SCAS with the spring topology as depicted in figure 3(a). This would no longer be the case if the fiber was positioned farther away from the springs. Hence we propose to include a pedestal on top of the spring to increase the distance between the spring and the bare G.652 SMF as depicted in figure 2(a). This topology allows us to

\[ \text{Figure 3. (a) An SMF exerting a force } P \text{ on a micro-spring with width } W \text{, thickness } T \text{ and length } L \text{ and (b) the mechanical model of this system assuming symmetry, with the reaction forces in black and the redundant reaction forces in red.} \]
design three and four-spring SCAS as depicted in figures 2(b) and (c) while obtaining reasonable spring constants. On the other hand, it is clear that a spring length in the order of a few mm is not desirable given the surface area of other commercial connectors, e.g. MPO-connector [17]. We choose to maximize the length within the available surface area to obtain compatibility with MPO-connectors, thus keeping the spring constant as low as possible to ensure easier fibre insertion. This leads to a length \( L = 890 \mu m \) for the three-spring and \( 690 \mu m \) for the four-spring SCAS, which in turn results in a spring constant of respectively \( 37.4 \text{kN m}^{-1} \) and \( 80.4 \text{kN m}^{-1} \) using (11).

However, adding a pedestal to the spring (further designated ‘pedestal-based’ spring) changes the stiffness of the spring and will increase its spring constant when compared to a ‘pedestal-free’ spring with the same length.

The pedestal-based spring can be approximated as shown in figure 4(a), where the pedestal’s width and height are \( 200 \mu m \) and \( 350 \mu m \) respectively. These dimensions are chosen to respectively obtain mechanical robustness and to ensure that the distance between the SMF and the spring is such that there is no overlap between the three or four different springs. To determine the reaction forces of this pedestal-based spring, the same method as discussed earlier can be used, albeit with a slight change. Again, the reaction forces of the right-hand side will be assumed redundant, as shown in figure 4(b). We now split the structure into three regions, each with its own moment of inertia \( I_n \). This allows us to define the elements of the flexibility matrix as a sum of the coefficients \( \delta_{ij} \) of (9) for each separate region.

\[
\delta_{ij} = \sum_{n=1}^{3} \int_{a_n}^{b_n} \frac{MM_j}{EI_i} \, dx. \tag{12}
\]

Here \( a_n \) and \( b_n \) are the boundaries of region \( n \). Because of symmetry, solving (8) gives the reaction forces on both sides of the pedestal-based spring. The vertical reaction force \( V_z = U_l = F/2 \), remains unchanged compared to the pedestal-free spring, since adding the pedestal doesn’t change the vertical forces. The reaction moment \( M_z \) is equal to \( U_2 \), where the full expression for \( U_2 \) can be found in the appendix. With the reaction forces known, Pasternak’s theorem can be used once more to determine the deflection of the micro-spring for a unity force applied in the center of the spring. The inverse of (13) gives us the spring constant of the pedestal-based spring.

\[
\begin{align*}
\delta &= \int_0^{L_1} \left( U_2 - \frac{P x}{2} \right) \left( \frac{L_1}{2} - x \right) \, dx \\
&\quad + \int_{L_1}^{L_2} \left( U_2 - \frac{P x}{2} \right) \left( \frac{L_2}{2} - x \right) \, dx \\
&\quad + \frac{U_2 L_2 (L_3 - L_4)}{2E I_1} + \frac{P L_1}{12} \left( L_1^2 - L_2^2 \right) \\
&\quad + \frac{U_2 \left( \frac{L_1}{4} - L_1 \right) L_2}{2E I_2} - \frac{P L_1}{48} + \frac{P L_2}{4} - \frac{P L_1}{3}. \tag{13}
\end{align*}
\]

The resulting spring constants obtained for spring lengths ranging from 500 \( \mu m \) to 1500 \( \mu m \) are given by the solid black lines in figure 5.

2.3. Finite element analysis

In addition to the analytical model, finite element analysis (FEA) was used to create a numerical simulation model of the micro-springs. To this end we used and compared two commercially available FEA software packages, namely COMSOL multiphysics and simulia FEA. A 2D and 3D static
When comparing the spring constant values obtained from simulations and theory, we can see that agreement is satisfactory for pedestal-free springs. For pedestal-based springs, a significant difference exists between theoretical and simulated spring constant, which ranges from 15% for a spring length of 1500 μm to 69% for a spring length of 500 μm. This large difference can be explained by the fact that the FEA models take compression and elongation into account (through Poisson’s ratio), while our theoretical model does not. Since the spring constant is higher for shorter springs (i.e. the spring is stiffer), there is relatively more compression, and thus a more pronounced effect of the Poisson ratio, when a force is exerted than for longer springs. Because the pedestal-free springs are less stiff, this effect only becomes noticeable for increasingly short springs, as can be seen in figure 5 for lengths shorter than 690 μm. The theoretical model overestimates the spring constant because, without the Poisson effect, compression along the width does not result in an elongation along the length of the spring and thus a smaller deflection is obtained due to an exerted force, resulting in a larger spring constant. The resulting spring constants from the FEA simulations are 5.2 × 10⁴ N m⁻¹ and 1.1 × 10⁵ N m⁻¹ for respectively the three- and four-spring design, which are both orders of magnitude above the minimal required spring constant for vibration endurance calculated in section 2.1.

In addition to the deflection for a given exerted force, we also calculate the stress distribution throughout the SCAS using the FEA simulations. For these models we use a geometry with tapered pedestals as depicted in figure 2 rather than the rectangular version depicted in figure 4(a). Rectangular pedestals are easier to work with in the theoretical model, but the tapered version is needed in practice since otherwise the pedestals would make contact with each other (in the four-spring topology) and thus not clamp the SMF upon insertion. An additional benefit from using tapered pedestals is the reduction in irradiation time for prototyping with deep proton writing (see section 3). For these reasons the width of the top of the pedestal is chosen as 50 μm. The (functional) difference between the two cases is very small however: FEA simulations show a difference of less than 1.2% in spring constant. The central opening between the pedestals of the three and four-spring structures is designed to be 121 μm in diameter, which is smaller than the nominal 125 μm diameter of a bare G.652 SMF. As mentioned in the introduction this allows the fiber to be self-centered upon insertion and kept in place by the micro-springs which are exerting a force on the fiber. The full SCAS was modeled in 3D using mesh cells of 7 μm to 90 μm with a growth rate of 1.4. The resulting first principal stress distribution after fiber insertion is shown in figures 6 and 7 for the three- and four-spring structure.

### Table 1. Spring constant of a PEI micro-spring obtained from FEA simulations (COMSOL) and the difference \( \Delta k \) compared to the theoretical value calculated using (11).

<table>
<thead>
<tr>
<th></th>
<th>3-spring design</th>
<th>4-spring design</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>( k ) (kN m⁻¹)</td>
<td>( \Delta k ) (%)</td>
</tr>
<tr>
<td>2D point load</td>
<td>40.7</td>
<td>8.8</td>
</tr>
<tr>
<td>2D fiber load</td>
<td>39.4</td>
<td>5.2</td>
</tr>
<tr>
<td>3D line load</td>
<td>38.4</td>
<td>2.4</td>
</tr>
<tr>
<td>3D fiber load</td>
<td>37.8</td>
<td>0.9</td>
</tr>
<tr>
<td>Theory</td>
<td>37.4</td>
<td>80.4</td>
</tr>
</tbody>
</table>

mechanical model of a micro-spring as depicted in figure 3(a) was created with the same boundary conditions (i.e. a clamped-clamped beam), where respectively a point load and a line load were applied on the spring. The dimensions of the designed micro-springs are: \( T = 500 \) μm, \( W = 100 \) μm and \( L = 890 \) μm or \( L = 690 \) μm for respectively the three-spring and four-spring topology (as discussed in section 2.2). We consider PEI as material, which is modeled with the following material parameters: \( E = 3.3 \) GPa, \( \nu = 0.4 \) (Poisson’s ratio) and \( \rho = 1270 \) kg m⁻³ (density) [31]. The material of the fiber is silica, modeled with \( E = 73.1 \) GPa, \( \nu = 0.17 \) and \( \rho = 2201 \) kg m⁻³ [21]. All simulations were performed with a triangular or tetrahedral mesh (for 2D and 3D FEA respectively) with a mesh cell size ranging from 2 μm to 27.5 μm with a growth rate of 1.4. A second set of simulations was performed where instead of a point or line load respectively a circle or cylinder (representing a bare G.652 SMF) was used to apply a body load on the spring in the model. The latter are designated as ‘fiber load’ in table 1. Increasing the load from 0 to 1 N in steps of 0.1 N, the displacement of the bottom-center-point of the spring is determined. The relation between the deflection of the spring and the applied force is the inverse of the spring constant \( k \). The resulting spring constants from both the 2D and 3D and both the point/line load and the fiber load simulations are given in table 1. In this table, the spring constants obtained from FEA simulations are also compared with the spring constant calculated using (11). The values given here are obtained from COMSOL multiphysics only, since differences with results obtained from simulia FEA are smaller than 1.2%. We can see that the values for the spring constant agree well for theory and FEA. Since the fiber load models require a lot more computing power and are numerically less stable (due to physical contact modelling) we perform the subsequent FEA simulations with the 3D line load model. Considering that both the three- and four-spring designs result in a spring constant which is 3 orders of magnitude larger than the minimal requirement for vibration endurance calculated in section 2.1, the error compared to the theoretical value being smaller than 3% is sufficiently accurate.

Having shown good agreement for a pedestal-free spring we will now use FEA simulations to determine the effect of adding a pedestal to the spring, as depicted in figure 4(a), as well as to assess the stress distribution in the structure. We calculate spring constants for pedestal-based springs with lengths ranging from 500 μm to 1500 μm and the results are given in figure 5. It can be seen that the increase in spring constant, due to the addition of the pedestal, is larger for smaller spring lengths. This can be explained by the dimensions of the pedestal being the same for all cases, thus making the relative increase in stiffness, by adding a pedestal, larger for the shorter springs.
respectively. We can see that the highest first principal stress is tensile in nature and is localized in the outer corners (the clamping points of the theoretical model in figure 3(a)) and in the lower-central region of the spring, where the deflection is the largest. Using FEA we have optimized the fillet radius $R_f$ (as defined in figure 6) of the outer three corners to 175 μm in order to minimize the first principal stress at these locations and minimize the irradiation time for prototyping (see section 3). A maximum first principal stress of 7.2 MPa and 11.7 MPa is found for the three and four-spring alignment structure respectively when a 125 μm-diameter fiber is inserted. The larger first principal stress for the four-spring design can be explained by the fact that the spring constant of the springs is higher, because their length is shorter. The fiber insertion results in the same deflection (2 μm) of the springs, thus resulting in a higher stress value for the stiffer springs. Considering the tensile yield strength of PEI, which is 105 MPa [31], there is a large safety margin before the PEI would yield while a fiber is inserted for both the three- and four-spring design. For PSU this is also the case since its tensile yield strength is 62 MPa [30] and the maximum first principal stress is 5.8 MPa and 9.3 MPa for respectively the three- and four-spring topology. Due to the high spring constant of the micro-springs the fiber’s displacement under a 5g, 5 kHz excitation is negligible, which ensures compatibility with the vibration endurance discussed in section 2.1.

3. Prototyping

To fabricate the designed SCAS we use deep proton writing (DPW), our in-house developed technology for rapid prototyping of micro-optical and micro-mechanical components [32]. The DPW process involves irradiating a polymer with a high-energy proton beam, which breaks the polymer chains in case a positive resist material such as PMMA is used. After irradiation the broken polymer chains in the irradiated zones are etched away in a chemical wet etching step. DPW is especially advantageous to use in this case, because the conical shape of the irradiated zones [33] makes it possible for the central opening to be larger than 125 μm at the back-side of the sample, while being smaller in the center of the structure. This allows for an easy and ‘guided’ insertion of a bare G.652 fiber. Even though Monte-Carlo simulations for insertion loss at a wavelength of 1310 nm showed that the four-spring design would be more robust against fabrication tolerances, we prototype the three-spring design because the stray radiation damage doesn’t allow for correct writing of a four-spring structure with a 50 μm diameter proton beam [34]. The material used for prototyping is PMMA since the DPW process has been optimized for this material and since it has similar properties as the target material PEI (both have $E = 3.3$ GPa [35]). A proton energy of 12 MeV is used and the deposited dose is 27 pC per step of 0.5 μm. Prototyping efforts were successful and microscope images of one of the prototypes in 0.5 mm thick PMMA can be seen in figure 8. After fabrication, the prototypes are dimensionally characterized using a Werth UA400 coordinate measuring machine. The measured geometry is fed back into the FEA to determine the spring constants of the individual springs. The spring constants of separate springs of the same SCAS, obtained by FEA, show a maximal difference of 5%. The central opening is measured to be $120.8 \pm 2.0$ μm (target: 121 μm). The MT-pin holes necessary for alignment between two SCCs or with a commercial MPO-connector, are $700.8 \pm 2.6$ μm (target: 700 μm).
These measurements are averaged over ten fabricated prototypes.

4. Self-centering connector assembly

The thickness of the DPW-fabricated prototypes is 0.5 mm as mentioned in section 3. Since the SCAS can only ensure accurate lateral alignment of the fiber, a pre-alignment plate is included to avoid angular misalignment. This micro-milled pre-alignment plate has a 200 \( \mu \)m diameter hole to accommodate the SMF and is placed at a distance of 4 mm from the SCAS, effectively limiting the maximal angular misalignment to 0.86°. The SCC-assembly shown schematically in figure 9 is made by inserting MT-pins in a PMMA prototype SCAS, after which a spacer structure of 4 mm thick is slid over the MT-pins. After adding the 1 mm thick pre-alignment plate and a 1 mm thick mounting plate (for subsequent mounting of the SCC in the interferometric setup as described in section 5), an ultraviolet (UV)-curable adhesive (Norland optical adhesive 68) is applied at the interfaces. The four parts are tightly pressed together and cured for 10 min using a Hamamatsu Lightningcure UV-light source with an irradiance of 225 mW cm\(^{-1}\). This means that all components in the assembly are aligned by the MT-pins. To create a female SCC the MT-pins are gently pulled out of the connector after curing.

5. Fiber insertion

After assembly, a bare and flat-cleaved SMF needs to be inserted. This is easily achievable since DPW-fabricated features possess a slightly conical shape which helps to guide the fiber inwards [32]. To protect the cleaved fiber from chip-off or other damage and facilitate insertion even further, the fiber tip is plasma-treated for 400 ms with a Fitel fusion splicer (model No. 182PM) rendering the edges of the fiber facet slightly curved and smooth while not affecting the fiber’s core. We first measure the force acting on the bare G.652 SMF during insertion in the SCAS using the setup depicted in figure 10. The bare fiber is clamped on the load cell which in turn is mounted on a 3-axis translation stage. With a positional accuracy of 10 \( \mu \)m and an insertion speed of 0.5 mm s\(^{-1}\) the fiber is inserted into the SCAS, which is mounted on a dedicated holder. The force exerted on the fiber is measured and recorded every 10 ms during insertion. The data of a representative measurement is shown in figure 11. When the fiber is not in contact with the SCAS, the force exerted on the fiber is null. The moment the fiber facet comes into contact with the SCAS it is first guided inwards and then the force on the fiber builds up until it is sufficiently high to initiate the SCAS to open up and allow entry to the fiber. To obtain a sufficiently large force, the fiber forms an increasingly large buckle which causes the fiber to thrust forward into the SCAS when it is released, causing the sudden decrease in the measured insertion force observed in figure 11. We have previously shown that repeated buckle formation in ferrule-less connectors can be controlled by designing a dedicated buckling cavity and that repeated buckling is not detrimental to the fiber [16]. After the sudden decrease in measured force, coinciding with the forward thrust, the fiber experiences an increasing amount of friction which results from an increasing contact surface with the conically-shaped central opening of the SCAS. When the fiber tip has completely traversed the SCAS, a more-or-less constant amount of friction is observed from then on. Twenty-two measurements were performed on three prototypes resulting in a measured peak insertion force of 0.104 ± 0.007 N and a constant friction force of 0.075 ± 0.009 N. With this information, we know that a force-budget of at least 0.11 N should be provided for successfully inserting a fiber in the SCC.

To control the fiber protrusion during fiber insertion (which plays an important role for longitudinal fiber misalignment),
an in-house built broad-spectrum interferometric setup is used in which the fiber insertion can be monitored in situ [36]. By observing only the interference fringes, the protrusion of the fiber tip can be controlled with a 2.5 μm accuracy (which can be further improved to ±0.1 μm by performing a full measurement). Once in position the fiber is glued to the pre-alignment plate with UV-curable adhesive. For the durability of the SCAS component it is important to know whether there is out-of-plane deformation of the micro-springs in addition to their in-plane deflection and if so, to quantify it in order to predict possible mechanical failure of the SCAS. To determine this, a full measurement is performed with the interferometric setup and the data is analyzed using the squared-envelope estimation function by sampling theory [37], giving a vertical and lateral resolution of 0.1 μm and 2.5 μm/pixel respectively. The measured surface profile for an SCAS with a fiber fully inserted, can be seen in figure 12 and shows that there is indeed a limited out-of-plane deformation of the springs: a height difference of ±7 μm can be observed for the springs. This information is fed back into the 3D mechanical FEA of the full SCAS in COMSOL in order to calculate the resulting stress distribution (as discussed in section 2.3). The FEA predicts that the maximum first principal stress present in an out-of-plane deformed PEI SCAS is 17.7 MPa and tensile in nature. Considering that the tensile yield strength of PEI is 105 MPa [31], there is a large safety margin before the SCAS would yield. For PSU this is also the case since its tensile yield strength is 62 MPa [30] and the maximum first principal stress predicted by the FEA is 14.3 MPa. Even though this out-of-plane deformation is not problematic in terms of robustness, it can be avoided by inserting the fiber ~7 μm farther than the surface of the SCAS and subsequently pulling it back such that the springs return to their original position.

6. Optical performance

To measure the optical insertion loss between two SCCs, we use a Thorlabs Pro 8000 WDM source to launch light at 1549.36 nm into a G.652 SMF and measure the power at the other end with a Newport 818-IR power meter. First the power through the fiber is measured and used as [38]. The fiber is subsequently cut in two and each loose fiber end is cleaved perpendicularly and afterwards plasma-treated as discussed in section 5. Afterwards the loose fiber-ends are inserted in SCCs according to the procedure outlined in section 5, controlling the protrusion of the fiber tip such that it is in the same plane as the front facet of the SCAS. 10 SCAS prototypes were used to assemble SCCs which were used to terminate fibers in this way. Insertion losses were measured for each pair of SCCs as well as for cross-connections between SCCs from different pairs (shown in figures 13(a) and (b)). The losses measured ranged from 0.68 dB to 4.65 dB. During the measurements it was noticed that pressing on the mated connectors in the longitudinal direction significantly improved the optical performance, which lead to the conclusion that no physical contact was made between the fiber facets in the connector. The reason for this is twofold: the different components that make up the SCC are not assembled perfectly parallel to each other and the DPW prototyping of the SCAS causes some surface rounding due to the wet etching development after the irradiation step. This causes the SCAS to lie 5–10 μm deeper than the surrounding substrate. To investigate this issue two SCC-assemblies were made in which the fiber facets were protruding ~15 μm and ~19.5 μm beyond front plane of the SCAS. For this connection an insertion loss of 0.18 dB was measured and after applying index matching gel (Thorlabs G608N3 with a refractive index of 1.4378 at 1550 nm) this improved to 0.1 dB. This shows that if the fiber protrusion is carefully controlled, a connection with grade B performance [39] could be achieved with this SCAS technology. Next to inter-mating two SCCs, we also measured the insertion loss between an SCC and an MPO connector. The best connection achieved with an SCC-MPO connection (shown in figure 13(c)) showed a loss of 1.11 dB. For this assembly an MPO-ferrule was polished such that it could be used for flat physical contact connections (notice that standard commercial MPO connectors are angled physical contact connectors). Since the polishing was not done perfectly and since a fiber needed to be cleaved perpendicularly and positioned in the non ideally polished MPO-ferrule, this high loss is due to an angular misalignment and a lack of good physical contact between the fiber facets in the SCC-MPO connection.

7. Towards low-cost replication

Being able to create prototypes is only the first step towards a practical implementation of the SCAS. To potentially achieve mass deployment, low-cost replication of the prototypes needs to be considered since DPW is neither a low-cost technology nor able to produce sufficient quantities of components. Therefore we took the compatibility with replication technologies such as micro-injection moulding and hot-embossing into account already in the design phase by limiting the aspect ratio of the alignment structure to 1:5. This approach facilitates both the mould formation [40] and the demoulding process of replicas which becomes excessively challenging for aspect ratios above 1:5. The fabrication of a metal mould from a DPW master component was investigated in collaboration with the Karlsruhe
Institute of Technology (KIT). Here DPW prototypes were used as sacrificial master components in an electroplating process that forms a nickel negative of the prototype [41]. With the nickel mould insert created successfully, the next step is to replicate the SCAS in high-performance polymers (PEI, PSU) by means of hot embossing [24, 28]. This replication effort is currently ongoing. The replicas will be characterized geometrically and their optical performance will be measured, allowing us to compare the replicas with the DPW prototypes. In addition, we are currently investigating the use of ultra-precision diamond tooling and micro-electron discharge machining for the direct fabrication of a metal mould insert for the replication of the SCAS. We will also look at how micro-injection moulding compares to hot embossing for the mass production of this SCAS.

8. Conclusion

To move past the fundamental limit in performance of mechanical splices imposed by the maximum tolerance of ±1 μm on the cladding diameter of a G.652 standard telecom single-mode fiber, we developed a micro-spring-based self-centering alignment structure (SCAS). The vibration endurance standard for optical connectors imposes a minimal spring constant value of 34.9 N m$^{-1}$ and 26.2 N m$^{-1}$ for each individual spring in respectively the three- and four-spring topology of the SCAS we propose. With robustness and low-cost replication in mind, the micro-springs were designed with the following dimensions: a thickness of 500 μm and a width of 100 μm. We identified PSU and PEI as the most promising candidate materials for the components, since they are compatible with environmental operation conditions of fiber connectors and with replication techniques like hot-embossing and micro-injection moulding. We used the force method to calculate the spring constant of the micro-springs, which led to a maximal length of 9.11 mm and 10.03 mm for the three- and four-spring topology respectively. A pedestal was included in the design and the lengths of the micro-springs were designed to be 890 μm and 690 μm respectively. This allows for compatibility with commercially available MPO-connectors. Several finite element analysis simulations were performed and showed good agreement (difference <3%) with the theoretical calculations of pedestal-free micro-springs. In case of pedestal-based micro-springs though, a larger difference (up to 69%) was observed because compression was not taken into account in the theoretical model. Prototypes of the SCAS were created using deep proton writing (DPW). The three-spring topology prototypes were successfully created, showing a difference in spring constant of maximally 5% between each individual spring of the same SCAS and a central opening of 120.8 ± 2.0 μm where the target was 121 μm. Since a fiber needs to be inserted in the alignment structure, we measured the force required to insert a fiber to be 0.11 N. This means that the bare fiber will buckle before fully entering the SCAS. The fiber insertion also causes an out-of-plane deformation of the micro-springs of ±7 μm. The FEA predicts that this is not a problem for robustness. The prototypes were then used to create connector-assemblies which were characterized in terms of optical performance. An insertion loss down to 0.1 dB is achievable in case care is taken to control the protrusion of the fiber in the alignment structure and in case index matching gel is applied. In addition, a metal mould was created from the DPW prototypes using electroplating [41] to assess the compatibility of our designed SCAS with the replication technology of hot embossing. This replication is currently ongoing and the reliability of the replicated parts will be characterized.

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Appendix. Theoretical calculation of reaction forces in a pedestal-based micro-spring

The full calculation of $U_2$ from section 2.2 can be found here. Figure 4(b) shows the model for which we want to determine the elements of (8), where $C_1$ and $C_2$ are 0.
\( \delta_{11} = \int_0^{L_1} \frac{(-)(L_3 - x)(-)(L_3 - x)}{E_l} \, dx + \int_0^{L_2} \frac{(-)(L_3 - x)(-)(L_3 - x)}{E_l} \, dx + \int_0^{L_3} \frac{(-)(L_3 - x)(-)(L_3 - x)}{E_l} \, dx \)

\[ = \int_0^{L_1} \frac{L_3^2 - 2L_3x + x^2}{E_l} \, dx + \int_0^{L_2} \frac{L_3^2 - 2L_3x + x^2}{E_l} \, dx + \int_0^{L_3} \frac{L_3^2 - 2L_3x + x^2}{E_l} \, dx \]

\[ = \frac{1}{E_l} \left[ L_3^2 - \frac{2L_3x^2}{2} + \frac{x^3}{3} \right]_{L_1}^{L_3} + \frac{1}{E_l} \left[ L_3^2 - \frac{2L_3x^2}{2} + \frac{x^3}{3} \right]_{L_2}^{L_3} + \frac{1}{E_l} \left[ L_3^2 - \frac{2L_3x^2}{2} + \frac{x^3}{3} \right]_{L_2}^{L_3} \]

\[ = \frac{1}{E_l} \left( L_3^2 - L_3L_1^2 + \frac{L_3^3}{3} + L_3L_2^2 - \frac{L_3^2}{3} \right) + \frac{1}{E_l} \left( L_3^2 - \frac{L_3L_2^2}{2} + L_3L_4^2 - \frac{L_3^2}{3} \right) \]

\[ (A.1) \]

\( \delta_{12} = \int_0^{L_1} \frac{(-)(L_3 - x)}{E_l} \, dx + \int_0^{L_2} \frac{(-)(L_3 - x)}{E_l} \, dx + \int_0^{L_3} \frac{(-)(L_3 - x)}{E_l} \, dx \)

\[ = -\frac{1}{E_l} \left[ L_3^2 - \frac{2L_3x^2}{2} + \frac{x^3}{3} \right]_{L_1}^{L_3} + -\frac{1}{E_l} \left[ L_3^2 - \frac{2L_3x^2}{2} + \frac{x^3}{3} \right]_{L_2}^{L_3} \]

\[ = \frac{1}{E_l} \left( L_3^2 - \frac{L_2^2}{2} + L_3^2 - \frac{L_2^2}{2} - L_3L_2 + L_3L_2^2 - \frac{L_3^2}{2} \right) \]

\[ = \frac{1}{E_l} \left( L_3^2 - \frac{L_2^2}{2} - L_3L_4 + L_4^2 \right) \]

\[ (A.2) \]

\( \delta_{22} = \int_0^{L_1} \frac{1 \times 1}{E_l} \, dx + \int_0^{L_2} \frac{1 \times 1}{E_l} \, dx + \int_0^{L_3} \frac{1 \times 1}{E_l} \, dx \)

\[ = \frac{1}{E_l} \left[ x \right]_{L_1}^{L_3} + \frac{1}{E_l} \left[ x \right]_{L_2}^{L_3} + \frac{1}{E_l} \left[ x \right]_{L_3}^{L_4} = \frac{L_3 - L_2 + L_3}{E_l} + \frac{L_4 - L_3}{E_l} \]

\[ (A.3) \]

\( \delta_{13} = \int_0^{L_1} \frac{(-)(L_3 - x)P(L_3 - x)}{E_l} \, dx + \int_0^{L_2} \frac{(-)(L_3 - x)P(L_3 - x)}{E_l} \, dx + \int_0^{L_3} \frac{(-)(L_3 - x) \times 0}{E_l} \, dx \)

\[ = \int_0^{L_1} \frac{P \left( \frac{L_3 - x}{2} - \frac{L_3}{2} + \frac{L_3}{2} + x^2 \right)}{E_l} \, dx + \int_0^{L_2} \frac{P \left( \frac{L_3 - x}{2} - \frac{L_3}{2} + \frac{L_3}{2} + x^2 \right)}{E_l} \, dx \]

\[ = -\frac{P}{E_l} \left( \frac{L_3^2}{2} - \frac{3L_3x^2}{4} + \frac{x^3}{3} \right)_{L_1}^{L_3} + -\frac{P}{E_l} \left( \frac{L_3^2}{2} - \frac{3L_3x^2}{4} + \frac{x^3}{3} \right)_{L_2}^{L_3} \]

\[ = \frac{P}{E_l} \left( \frac{L_3^2L_4}{2} - \frac{3L_3L_2^2}{4} + \frac{L_3^3}{3} \right) + \frac{P}{E_l} \left( \frac{5L_3^2}{48} - \frac{3L_3L_2^2}{4} + \frac{L_3^3}{3} \right) \]

\[ (A.4) \]

\( \delta_{23} = \int_0^{L_1} \frac{1 \times 1 \times P(L_3 - x)}{E_l} \, dx + \int_0^{L_2} \frac{1 \times 1 \times P(L_3 - x)}{E_l} \, dx + \int_0^{L_3} \frac{1 \times 0}{E_l} \, dx \)

\[ = \frac{P}{E_l} \left( \frac{L_3^2L_4}{2} - \frac{L_2^2}{2} \right) + \frac{P}{E_l} \left( \frac{L_3^2}{8} - \frac{L_3L_2}{2} + \frac{L_2^2}{2} \right) \]

\[ (A.5) \]

\( U_2 \) can be written as a function of the elements of the flexibility matrix \( \delta_{ij} \) and \( \delta_{ip} \).

\[ \begin{cases} 
\delta_{11}U_1 + \delta_{12}U_2 = -\delta_{1p} \\
\delta_{21}U_1 + \delta_{22}U_2 = -\delta_{2p} 
\end{cases} \]

\[ U_1 = \frac{\delta_{1p}}{\delta_{11}} - \frac{\delta_{12} \delta_{2p}}{\delta_{11} \delta_{22} - \delta_{12}^2} \]

\[ U_2 = \frac{\delta_{2p}}{\delta_{12}} - \frac{\delta_{1p} \delta_{12}}{\delta_{11} \delta_{22} - \delta_{12}^2} \]
With the reaction forces and thus \( U_2 \) known (as shown in figure A1), we can calculate the deflection \( \delta \) of the micro-spring for a unity load applied in its center.

\[
\delta = \int_0^{L_2} \left( U_2 - \frac{Pu}{2} \right) \left( L_2 - x \right) \frac{EI}{UL} dx + \int_0^{L_2} \left( U_2 - \frac{Pu}{2} \right) \frac{EI}{UL} \left( L_2 - x \right) dx + \int_0^{L_2} \frac{EI}{UL} \left( U_2 - \frac{Pu}{2} \right) \frac{EI}{UL} \left( L_2 - x \right) dx
\]

\[
= \frac{1}{2EI} \left[ U_2L_3 + \left( U_2 + \frac{PL_3}{4} \right) x^2 + \frac{Pu}{2} x^3 \right]_{x=0}^{L_2} + \frac{1}{2EI} \left[ U_2L_3 + \left( U_2 + \frac{PL_3}{4} \right) x^2 + \frac{Pu}{2} x^3 \right]_{x=0}^{L_2}
\]

\[
= \frac{1}{2EI} \left( U_2L_3 - U_2L_3 + \frac{PL_3}{4}L_3^2 + \frac{Pu}{2}L_3^3 \right) + \frac{1}{2EI} \left( U_2L_3 - U_2L_3 + \frac{PL_3}{4}L_3^2 + \frac{Pu}{2}L_3^3 \right)
\]

\[
= \frac{1}{2EI} \left( \frac{PL_3}{4}L_3^2 + \frac{Pu}{2}L_3^3 \right)
\]

\[
(A.7)
\]
fibers to surface-active optoelectronic components Sensors
Actuators A 82 245–8


[38] International Electrotechnical Commission 2012 Fibre optic interconnecting devices and passive components—basic test and measurement procedures—part 3–4: examinations and measurements—attenuation (IEC 61300-3-4)

