SENSORS, IDENTIFICATION, AND LOW LEVEL CONTROL OF A FLEXIBLE ANTHROPOMORPHIC ROBOT HAND

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The successful control of a robot hand with multiple degrees of freedom not only requires sensors to determine the state of the hand but also a thorough understanding of the actuator system and its properties. This article presents a set of sensors and analyzes the actuator properties of an anthropomorphic robot hand driven by flexible fluidic actuators. These flexible and compact actuators are integrated directly into the finger joints, they can be driven either pneumatically or hydraulically.

The sensors for the measurement of joint angles, contact forces, and fluid pressure are described; the designs utilize mostly commodity components. Hall sensors and customized half-ring rare-earth magnets are used to integrate the joint angle sensors directly into the actuated joints. A force sensor setup allowing soft finger surfaces is evaluated. Fluid pressure sensors are needed for the model-based computation of joint torques and to limit the actuator pressure.

Static and dynamic actuator characteristics are determined in a theoretical process analysis, and suitable parameters are identified in several experiments. The resulting actuator model incorporates the viscoelastic material behavior and describes the relations of joint angle, actuator pressure, and actuator torque. It is used in simulations and for the design of a joint position controller.

Keywords: Anthropomorphic robot hands; flexible fluidic actuators; sensors; control.

1. Introduction

Artificial hands are an essential contribution to enable a humanoid robot with real-world interaction and cooperation. Direct interaction with humans and human environments poses several challenges to the design of suitable artificial hands.

We have earlier presented an approach for such an anthropomorphic, light-weight five-fingered hand with inherent compliance using flexible fluidic actuators integrated into the finger joints.1–4 These actuators are closed elastic chambers; they can be inflated either pneumatically or hydraulically resulting in a joint torque.
Fig. 1. Concept for an integrated hydraulic five-finger-hand.

Our hand prototype incorporates an aluminum skeleton with 11 DOF, each finger has two joints for flexion; the thumb is equipped with a third joint to allow opposition with other fingers. Previous sensorless prototypes of this hand (Fig. 1) already allowed the successful, time-controlled execution of various prehensile patterns like cylindrical grasp, tripod grasp and lateral grasp.

Improvements in grasping and manipulation using this actuator technology require sensors and advanced control strategies. In the following section, we will present a set of sensors compliant with our hand design to obtain feedback on the hand’s state and its interaction with the environment. The last part of this paper covers an actuator model describing the actuator properties relevant for position and torque control as well as the controller itself.

2. Sensor System

Three classes of sensors are considered in this paper: joint angle, contact force and fluid pressure. Besides compatibility with the existing hand concept, the development focused mainly on simplicity and the use of reliable commodity components.

2.1. Joint angle sensors

Many researchers have already addressed the issue of finger joint angle measurement. Depending on the chosen actuator principles, either finger-integrated or external sensors were chosen. Typical integrated position sensors were conductive plastic potentiometers,\(^5\) optical position sensing devices\(^6\) or hall-sensors with permanent magnets mounted to a slider mechanism.\(^7,8\) In all these cases, the actuators were not directly integrated into the finger joints but into the phalanges. Especially for fingers driven externally via tendons\(^9\) or ball-spindle drives,\(^10\) DC motors with optical
encoders were chosen. Such external sensors typically did not have to satisfy hard package constraints.

For finger joints with integrated flexible fluidic actuators, a sensor fitting directly into the joint mechanism with a flexion axis close to the palmar side of the fingers (Fig. 2) was needed. The developed sensor relies on two main principles:

- the magnitude of flux density along the perimeter of a circular permanent magnet with diametrical magnetization is constant;
- the output signal of a Hall sensor is sensitive to the direction of the magnetic field and proportional to the normal flux density.

A Hall sensor to measure the normal flux density $B_n$ of a rotating diametrically magnetized disc magnet at a constant position results in a sinusoidal signal as in Eq. (1). The signal has a maximum normal flux density $B_0$ along the magnetization axis and an offset angle $\phi_0$ between the magnetization axis and the hall element’s normal axis:

$$B_n = B_0 \cdot \sin(\varphi - \varphi_0).$$

With the finger joint having a range of motion of $-10^\circ \leq \varphi \leq +100^\circ$ and $\varphi_0 = 45^\circ$, we obtain a signal as in Fig. 3.

To minimize the effects on joint structure stability, the magnet is reduced to a half-ring shape resulting in a setup and signal as in Fig. 4. As a consequence, a lower signal amplitude and an increased nonlinearity at extreme angles is obtained.

The sensor signals from simulations and prototypes were approximated with a first-order polynomial equation. The sensor nonlinearity $l$ was then evaluated using Eq. (2) as suggested in Ref. 7 with $n$ measured signal values $U_m$ and approximated values $U_I$ giving the results in Table 1:

$$l = \frac{100\%}{|U_{m,n} - U_{m,1}|} \cdot \sqrt{\frac{\sum_{i=1}^{n}(U_{m,i} - U_{I,i})^2}{n}}.$$
Fig. 3. 2-D FEM magnet simulation setup (left) and output signal (right) of a hall sensor moved along the perimeter of a ring magnet with \( \phi_0 = 45^\circ \).

Fig. 4. 2-D FEM magnet simulation setup (left) and output signal (right) of a hall sensor moved along the perimeter of a half-ring magnet with \( \phi_0 = 45^\circ \).

Table 1. Measured sensor signal linearity \( l \).

<table>
<thead>
<tr>
<th>Joint range of motion</th>
<th>Simulation</th>
<th>Measurement</th>
</tr>
</thead>
<tbody>
<tr>
<td>( 0^\circ \cdots 90^\circ )</td>
<td>( -10^\circ \cdots 100^\circ )</td>
<td>( 0^\circ \cdots 90^\circ )</td>
</tr>
<tr>
<td>Ring magnet</td>
<td>0.014%</td>
<td>0.039%</td>
</tr>
<tr>
<td>Half-ring magnet</td>
<td>0.039%</td>
<td>0.117%</td>
</tr>
<tr>
<td>Slider mechanism ( ^7 )</td>
<td>( 3.8 \cdots 5.3% )</td>
<td>3.8%</td>
</tr>
</tbody>
</table>

As expected, the half-ring magnet sensor version performs worse than the full-ring version and the actual prototypes perform worse than the corresponding simulation. However, even the half-ring version presents a more than 10 times lower nonlinearity than the comparable slider mechanism while being used over an extended range of motion \( \(-10^\circ \cdots 100^\circ\)\).
The employed ratiometric hall sensor with an integrated amplifier and a temperature compensation provides an output signal \( U_{\text{out}} \) as in Eq. (3), which can be fed directly into an analog/digital converter. \( U_0 \) denotes the sensor supply voltage, and \( B_{\text{max}} \) is the maximum flux density that can be handled by the amplifier:

\[
U_{\text{out}} = \frac{U_0}{2} \left( 1 + \frac{B_n}{2 \cdot B_{\text{max}}} \right).
\]  

(3)

Output signals covering the range from approximately 0.5 V to 4.5 V can be obtained using rare-earth NdFeB magnets and a suitably chosen distance between magnet and hall sensor.

An optimization of the signal linearity was performed using a genetic algorithm implemented in Matlab and FEMM\textsuperscript{11} as a 2D-FEM magnet simulation tool. The steady-state genetic algorithm derived from Ref. 12 with a population size of 25 uses tournament selection. Its genotypes describe the radius of the magnet contour in steps of 10\(^\circ\). A smooth magnet contour is obtained by computing the in-between points in steps of 1\(^\circ\) using:

\[
r(\phi) = r_0 + \sum_{i=1}^{n} g_i \cdot e^{\left(\frac{g_i \cdot (\alpha - \phi)^2}{2 \cdot \sigma^2}\right)}.
\]  

(4)

As expected, we see an increased magnet radius in the outer areas reducing the distance between the magnet and the hall sensor resulting in a higher measured flux density (Fig. 5, left). The resulting magnet contour is shown in Fig. 5, right.

![Magnet contour](image)

Fig. 5. Geometry optimization results: magnet radius (left) and magnet contour (right).

Figure. 6 demonstrates the optimized signal curve and the position error estimated from the simulation results. Such a linear sensor signal simplifies the calibration process for newly assembled joints to a linear interpolation between the values measured at the two end positions (0\(^\circ\) and 90\(^\circ\)).

### 2.2. Contact force sensors

Force sensors\textsuperscript{13,14} are essential for the detection of object contact during grasping. Numerous types have been used in artificial hands ranging from binary switches
For the use in our hand, force sensors had to fulfill three central criteria: minimized thickness to fit between the bone structure and the palmar hand surface, compliance with a soft palm to increase grasp safety and a high resolution for forces below 5 N.

Among the conductive polymer sensors, force sensing resistors (FSR, Interlink Electronics) are a well-known and cheap type of sensor not only used in robotics but also in force measurement on humans in programming by demonstration environments and physiology. These sensors are made of three coated plastic film layers giving an overall thickness of about 0.5 mm. The bottom layer is coated with a semiconductive layer; the top layer is printed with two interdigitated conducting structures. A spacer layer in the outer sensor areas separates these two layers. We found a FSR with an outer diameter of 7.62 mm (type FSR-149, Fig. 7, right) to be the most suitable for the integration into the fingers of a hand approximating human size.

The basic sensor characteristic approximates a negative linear relationship between the logarithms of the applied force $F$ and the resistance $R_S$ (Eq. (5) and Fig. 7, left):

$$\log R_S \approx c_0 - c_1 \cdot \log F.$$

This nonlinear relationship can be exploited with a voltage divider setup (Fig. 8, left) measuring the output voltage $U_D$ at the fixed resistor $R_D$. A high sensitivity is obtained in the force range between 0 and 5 N while larger forces only result in small signal changes (see Fig. 8).

The best way to compute the applied force from the voltage divider output signal was to determine the FSR resistance from the supplied voltage and the fixed resistor $R_D$ and to approximate Eq. (5) with a third-order polynomial using least-squares regression. The calibration data was obtained from a test rig with a digital lab scale as a reference force sensor.

The issues of well-defined actuation, sensing of small contact forces, mechanical sensor protection against sharp edges, and a soft finger surface are resolved...
Fig. 7. Left: force-resistance diagram of FSR sensor. Right: FSR sensor, inside view of silicone covers for phalanges and finger, phalanx with FSR sensor and silicone cover.

Fig. 8. FSR force sensor in a voltage divider setup. Left: schematic with unity gain buffer circuit. Right: force-voltage diagram.

with sensor covers as depicted in Fig. 7, right. A prototype aluminum mold is used to manufacture the sensor covers from medical two-component silicone (Detax Durosil). The sensor is actuated with a sphere of 4 mm diameter. For the fingertips, the outer cylindrical shape ends with a spherical fingertip. For physiological measurements, this actuation principle was modified for the attachment to human and prosthetic fingers.\textsuperscript{19}

2.3. Actuator pressure sensor setup

In a fluidically driven artificial hand, pressure sensors are essential for a number of functions such as position control, torque estimation, and monitoring to prevent actuator damage. Tests have shown that for pneumatics a pressure measurement in or near the actuators is not necessary for our requirements. This allows one to integrate the pressure sensors into future valve units in the hand’s palm with less strict package requirements and easier wiring.
We will see in Sec. 3 that in pneumatic applications, the joint position should be controlled by setting an actuator pressure, so this method requires a low-level pressure control loop which is accomplished with an analog pressure control valve. For our type of actuators, the actuator torque can be estimated from the measured actuator pressure and the joint angle.

3. Modeling and Low-Level Control

In this section, an actuator model is derived from theoretical analysis and measurements performed on prototype parts. A pneumatic joint position controller is deduced from this actuator model, followed by considerations on future controller developments.

3.1. Static actuator characteristics

To obtain the static actuator torque characteristics $M_a(\varphi, p_a)$, static experiments were performed in a test rig. The joint torque is measured using an industrial-grade torque sensor for the given joint angles $\varphi$ set by a step motor and actuator pressures $p_a$ set with a pneumatic pressure-control valve. The values obtained are shown in Fig. 9, left.

Multilinear regression was used to compute the parameters $a_0$ to $a_3$ in Eq. (6) used later as a static actuator model (Fig. 9, left):

$$M_a(p_a, \varphi) = a_0 + a_1 \cdot \varphi + a_2 \cdot p_a + a_3 \cdot p_a \cdot \varphi.$$  

(6)

During these experiments, we also determined the actuator volume characteristics $V_a(\varphi, p_a)$.

Fig. 9. Measured (left) and approximated (right) static actuator torque characteristics.
3.2. Actuator compliance

The previously described torque behavior is independent of the chosen fluid. However, the stiffness properties vary significantly between pneumatics and hydraulics. Torque trajectories of an actuator filled with constant fluid amounts were computed from the torque \( M_a(\varphi, p_a) \) and volume \( V_a(\varphi, p_a) \) characteristics. In the case of pneumatics (Fig. 10, left), we approximate the fluid behavior using Boyle–Mariotte’s law for a constant mass \( m \) (i.e. a closed valve) of an ideal gas at constant temperature \( T \):

\[
p_a \cdot V_a = \text{const.} \quad | T=\text{const.}, m=\text{const.}
\]

Here, a soft behavior can be observed when opening the finger with an external torque. A large portion of the trajectories reaches \( \varphi = 0^\circ \) with a moderately increased pressure.

For hydraulics (Fig. 10, right) and the chosen pressure range, we can assume an incompressible fluid with a pressure-independent volume, i.e. any observed compliance depends solely on the actuator. Here, the system behaves much more stiffly and we can see that most trajectories reach the maximum actuator pressure \( (6 \times 10^5 \text{ Pa}) \) before the joint is fully opened. Such a compliant actuator behavior can be considered favorable during grasping, as the fingers will follow object position disturbances without controller activity or an immediate loss of object contact.

![Fig. 10. Torque trajectories of constant fluid masses for pneumatics (left) and hydraulics (right) computed from experimental results.](image)

3.3. Dynamic behavior

More effects have to be taken into consideration for the dynamic properties of the actuator. From assuming a model structure (Fig. 11) with a speed-proportional friction \( d \), an inertia \( J \) of the distal phalanx and a disturbance torque \( M_d \), we obtain the differential equation:

\[
M_a(p_a, \varphi) = J \cdot \ddot{\varphi} + d \cdot \dot{\varphi} + M_d .
\]

(8)
However, measurements with predefined pressure trajectories and $M_d = 0$ show obvious differences to the simulation (Fig. 13), especially a hysteresis and a time-dependent behavior, which can be attributed to the actuator material.

Contrary to metals, polymer materials do not show a linear elastic behavior ($\sigma = \varepsilon \cdot E$). Rather, an asymptotic increase in strain $\varepsilon(t)$ at a constant tension $\sigma$ or an asymptotically decreasing tension $\sigma(t)$ can be observed at constant strain $\varepsilon$. Such a reversible viscoelastic behavior is typically described in the literature with spring-damper models. In the simplest case, this is accomplished with a series of a spring and a second spring with a parallel damper, thus resulting in

$$D(t) = \frac{1}{E(t)} = D_0 + c \cdot [1 - e^{-t/\tau_i}]. \quad (9)$$

This model can also be expanded to a more exact equation (10) using the non-negative function $f(\tau)$ as a retardation spectrum:

$$D(t) = D_0 + \int_0^\infty f(\tau) \cdot [1 - e^{-t/\tau}]d\tau. \quad (10)$$

An approach allowing to model arbitrary pressures $p(t)$ can be derived using the following assumptions:

- the material retardation only affects the pressure-independent part of the actuator torque;
- a creeping ratio $A$ with $0 \leq A \leq 1$ is used to model the pressure influence;
- the current creeping ratio results in a displacement of $M_a(p_a = 0, \varphi) = a_0 + a_1 \varphi$ in $\varphi$. Positive values of $A$ result in reduced actuator reverse forces and therefore increased joint angles;
- the maximum retardation-dependent angle $\Delta \varphi_{\text{max}}$ limits the displacement of $\varphi$.

Hence, we obtain a modified actuator model:

$$M_a(p_a(t), \varphi) = a_0 + a_1(\varphi + A \cdot \Delta \varphi_{\text{max}}) + a_2 p_a + a_3 p_a \varphi. \quad (11)$$

For the integration into the modified actuator model, a material model was designed to describe the hysteresis and the creeping behavior. Figure 12 (left) shows...
three zones of recovery, constant creeping ratio, and extension separated by $p_0(A)$ and $p_1(A)$, the right graph of Fig. 12 denotes $\dot{A}$, i.e. the rate at which $A$ changes:

$$
\begin{align*}
\frac{b_0}{b_3} \dot{A} + A &= b_1 \cdot (p_a - p_{\text{min}}) \quad \text{for } p_a \geq p_1(A), \\
\dot{A} &= 0 \quad \text{for } p_0(A) < p_a < p_1(A), \\
\frac{b_0}{b_2} \dot{A} + A &= b_0 \cdot p_a \quad \text{for } p_a \leq p_0(A).
\end{align*}
$$

(12)

Solving the mathematical description of these graphs in Eq. (12) for $A(t=0) = A_0$ and a pressure step to $p_a$ at $t \geq 0$, we obtain

$$
A(t) = \begin{cases} 
  b_1 \cdot (p_a - p_{\text{min}}) \cdot \left[1 - e^{-\frac{b_3}{b_1} t}\right] + A_0 \cdot e^{-\frac{b_3}{b_1} t} & \text{for } p_a \geq p_1(A), \\
  A_0 & \text{for } p_0(A) < p_a < p_1(A), \\
  b_0 \cdot p_a \cdot \left[1 - e^{-\frac{b_2}{b_0} t}\right] + A_0 \cdot e^{-\frac{b_2}{b_0} t} & \text{for } p_a \leq p_0(A).
\end{cases}
$$

(13)

We obtain a structure identical to Eq. (9), which allows to match the parameters. Using a suitable set of parameters $\Delta\psi_{\text{max}}, p_{\text{min}},$ and $b_0, \ldots, b_3$, we obtain an optimized actuator model behaving as in Fig. 13, right.
3.4. Pneumatic control

The previously described model is the basis for the design of a closed-loop joint position controller. A discrete PID controller implemented in LabView was enhanced with a series of measures:

- adaptation to compensate the non-linear system behavior;
- anti-reset-windup integral action and output limiters;
- speed up with additional, model-based feed-forward control using the reference variable;
- dead-band to reduce valve activity.

The controller’s performance with and without adaptation is demonstrated in Fig. 14. Due to its design, the position controller will respond to permanent angular deviations with an actuator pressure rising continuously to the system pressure. This behavior can be considered as undesirable especially for the grasping of delicate objects, as it would result in grasping forces only limited by \( M_a(\phi; p_{max}) \). We are therefore evaluating a torque-limited position control with a maximum torque as a second feed-forward reference variable. The actuator model is then used to dynamically limit the maximum actuator pressure depending on the joint position. This feature is discussed in Ref. 21 together with a model-based approach for object contact detection.

![Fig. 14. Comparison of controller behavior with and without adaptation.](image)

4. Conclusion and Outlook

We have presented a set of low-cost sensors for the integration into an artificial anthropomorphic hand driven by fluidic actuators. A joint angle sensor based on hall sensors and custom-made rare-earth magnets was evaluated for its output signal properties, linearity, and options for further size reduction. An integration approach for FSR sensors was described considering sensor linearity issues, the detection of low contact forces, and the realization of a soft finger surface especially. Reasons for the use of pressure sensors were given in the context of an actuator model describing the dependencies between actuator pressure, joint angle and the exerted torque. Dynamic actuator characteristics were derived from a theoretical analysis.
and measurements. A controller design and a number of optimizations were outlined and an outlook on further improvements was given.

Future work will cover the port of the current controller algorithm to a microcontroller and the integration of a pressure control loop to replace the analog pressure control valve. Hydraulic joint position and torque control will be developed to reduce the unit’s size and weight. Model-based contact detection during joint movements will be evaluated in comparison to direct contact force measurements. Independently, methods for high-level finger coordination and hand control are being evaluated to allow a number of different grasp patterns and the coordination with arm movements.

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References

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