Z-Axis Magnetometers for MEMS Inertial Measurement Units Using an Industrial Process

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Abstract—The combination of multiaxis and multiparameter microelectromechanical systems (MEMS) in the same technology would result in very cheap and smart inertial measurement units (IMUs). In this paper, a Z-axis Lorentz-force-based magnetometer whose design and optimization are reviewed taking into account the constraints of an industrial MEMS technology (process and packaging) already used for accelerometers and gyroscopes is presented. How this impacts the design guidelines is shown; in particular, a very compact device that can fit in the same package of the gyroscope to realize an all-MEMS seven-degree-of-freedom IMU is proposed and experimentally tested. The device shows a mechanical sensitivity of around 0.8 aT/(μT·mA) with a resolution of 520 (nT·mA)/√Hz over a signal bandwidth of 50 Hz at room temperature. Coupled to a transimpedance amplifier, the system shows an overall sensitivity of 150 μV/μT at 250 μA of peak driving current.

Index Terms—Inertial measurement units (IMUs), magnetometers, microelectromechanical systems (MEMS), motion control, smart sensors.

I. INTRODUCTION

T

HE REALIZATION of an inertial measurement unit (IMU) based on multiparameter and multiaxis microelectromechanical systems (MEMS) devices integrated in the same chip is of great interest for several fields of application, including civil and military aviation, space satellites, trains, ships, unmanned or remotely operated vehicles, stabilization systems, consumer electronics, and several others [1]–[5]. In particular, the integration of a three-axis accelerometer, a three-axis gyroscope, a three-axis magnetometer, and a pressure sensor, all based on the same MEMS process, can result in a “10-DOF” high-resolution, low-cost, and low-power miniaturized system for position, motion, heading, and altitude monitoring. The state of the art is represented by a six-axis MEMS unit for acceleration and angular rate sensing [6], [7].

Magnetometers of IMUs are instead still based on non-MEMS technologies. For instance, the multiaxis systems for motion and magnetic field sensing described in [8] and [9] include accelerometers or gyroscopes based on MEMS technology but magnetometers based on anisotropic magnetoresistance (AMR) [10].

In this context, several research efforts are put in developing MEMS magnetometers [11]–[13]. In particular, as the AMR technology is sensitive only to in-plane fields, the magnetometer for the Z-axis is the same as for the X- and Y-axes but vertically twisted by 90°. Large volume is then wasted in height due to the required out-of-plane assembling, reducing the integrability of the IMU in ultrathin plastic packages. Therefore, most of the referenced research focuses on the development of a Z-axis MEMS magnetometer.

In a very interesting intermediate step, a 9-DOF IMU can be realized with a two-axis AMR magnetometer and a 7-DOF MEMS die. In this paper, we thus focus on the design of Z-axis Lorentz-force capacitive magnetometers operating at resonance in a typical industrial packaging, for consumer applications (digital compass, heading, dead reckoning, and map rotation). The requirements are a resolution around a few microteslas over a bandwidth lower than 50 Hz, with a full-scale linear range of ≈1 mT. Although the working principle of this kind of magnetometer is already known [13], it is here put in the context of all the constraints imposed by the industrial process, packaging [14], and application.

1) The maximum device dimension should fit in the typical MEMS die (≈1 mm excluding dead areas).
2) The required vacuum level should cope with industrial packages (typical minimum pressure of ≈0.5 mbar).
3) The resonance frequency should be preferably out of the acoustic bandwidth (> 20 kHz).
4) Power dissipation should be competitive with respect to AMR technology (less than a few milliwatts).
5) Operation should be guaranteed within a temperature range of −40 °C to 85 °C.

Through a deep theoretical analysis, this paper reports novel guidelines for the optimum design of MEMS magnetometers considering all the constraints set earlier. In particular, it is demonstrated in Section II that the mechanical sensitivity of a parallel-plate magnetometer, defined as the capacitance variation per magnetic field change ΔC/ΔB, is independent of the number N of designed differential parallel-plate cells, when the free-molecule flow regime applies [15]. This is the case of industrial technologies, which can reach packaging pressures on the order of 1 mbar and feature minimum air gaps g on the
order of a few micrometers. As the pressure, the minimum air-gap dimension, and the process thickness are defined, there is practically only one way to increase the sensitivity, which is increasing the length \( L \) of the magnetometer springs.

In order to verify the said dependences, a custom boundary element model to estimate the damping in a parallel-plate cell is used. The results are then incorporated in a behavioral Simulink description of the magnetometer. Following the simulations, the design of four magnetometers, based on the STMicroelectronics process constraints and differing only in the cell number \( N \), is presented in Section III. In Section IV, a comparison between experiments and simulations shows a good agreement, confirming the modeling theory. The effect of possible changes in the damping coefficient due to process variance and/or temperature variations is also discussed.

The obtained result has an important effect on the device design, as it suggests the implementation of a \( Z \)-axis magnetometer with a high aspect ratio, very long in the spring direction, and very compact along the other axis, owing to the reduced number of plates. Such a design can easily fit in the same package of the accelerometers and gyroscopes, for a cost-effective design of a seven-axis MEMS IMU. The performance of sensitivity and resolution measured in this work, together with some considerations about power dissipation in the device and in the readout electronics, confirm the interest around the proposed approach.

II. DEVICE DESCRIPTION

There are several examples of MEMS magnetometers based on the Lorentz-force principle in the scientific literature. In all these devices, in the presence of a magnetic field \( B \), a driving current \( I \), flowing in a suspended structure orthogonally to the direction of \( B \), determines a force in a direction orthogonal to the plane of both \( B \) and \( I \). Although the induced motion can be sensed in different ways (e.g., through the change of resistance in piezoresistors [11] or through the change in the resonance frequency of suitably designed resonators [12]), the most used technique is capacitive readout [13]. Fig. 1 shows a schematic view of a typical \( Z \)-axis parallel-plate MEMS magnetometer. Two beams with a length \( L \) constitute the suspending element of a central frame, which forms, through a set of fixed stators, two differential parallel-plate sensing capacitors \( C_1 \) and \( C_2 \). For an ac current \( I(t) = i_0 \cdot \sin(2\pi f_0 \cdot t) \) flowing through the springs, as depicted at the device resonant frequency \( f_0 \), the structure is subject to the Lorentz force \( F_L \) on each spring and, thus, to a displacement amplified through the quality factor \( Q \)

\[
x(t) = \frac{2 \cdot F_L(t) \cdot Q}{2 \cdot k} = \frac{I(t)B \cdot L \cdot Q}{2 \cdot k}
\]

where \( k \) is the device stiffness. The factor “2” at the denominator accounts for the fact that the force is distributed across the whole length \( L \) and not concentrated on the shuttle.

The expression of the differential capacitance variation for a displacement \( x(t) \), in the assumption that displacements are much lower than the nominal air gap between parallel plates \( g \), can be written as [24]

\[
\Delta C(t) = 2 \cdot C_0 \cdot \frac{x(t)}{g} = 2 \epsilon_0 \cdot A_C \cdot \frac{x(t)}{g^2} = 2 \epsilon_0 \cdot L_C \cdot N H \cdot \frac{x(t)}{g^2}
\]

where \( \epsilon_0 \) is the electrical permittivity inside the package (assumed as that of vacuum) and \( A_C \) is the overall facing area of the parallel plates, which is equal to the product of the process height \( H \), the length of each parallel plate \( L_C \), and the number of differential sensing cells \( N \). The mechanical sensitivity \( \Delta C / \Delta B \) of the magnetometer, defined as the differential capacitance variation per variation of magnetic field, can be written as a function of the resonance frequency as

\[
\frac{\Delta C}{\Delta B} = 2\epsilon_0 L_C N H \cdot \frac{I(t) \cdot L \cdot Q}{g^2 \cdot 2 \cdot k} = \epsilon_0 L_C N H \cdot \frac{I(t) \cdot L}{g^2 \cdot 2\pi f_0 \cdot b}.
\]

In the formula above, \( b \) is the damping coefficient: It is known that this coefficient depends linearly on the pressure in the so-called transition regime, where the free-molecule flow applies [15]. This means that the damping is mostly caused by the collisions between the structure and the gas molecules, with a negligible interaction between the molecules themselves. As a consequence, as the largest number of collisions occurs between the parallel plates and the squeezed fluid, the coefficient \( b \) is proportional both to the area \( A_C \) and to the number \( N \) through a normalized damping coefficient per unit area \( b_{\text{area}} \)

\[
b = 2 \cdot b_{\text{area}} \cdot N \cdot L_C \cdot H
\]

(the factor “2” accounts for the differential configuration). It thus turns out that the sensitivity defined previously is independent of the number of parallel-plate cells

\[
\frac{\Delta C}{\Delta B} = \frac{\epsilon_0 \cdot I(t) \cdot L}{4\pi \cdot g^2 \cdot f_0 \cdot b_{\text{area}}}.
\]

Looking at the derived formula, some considerations can be made about possible optimization of the design.

1) The minimum air gap \( g \) is set by the technology. Choosing the minimum value means increasing the variance and
decreasing the repeatability from part to part, making calibration required [16], [17].

2) The resonance frequency $f_0$ is set in the range 20–30 kHz to avoid acoustic interference, as commonly done for gyroscopes [18], [19].

3) The damping coefficient per unit area $b_{\text{area}}$ is constrained by the packaging pressure and by the open-loop bandwidth of the device $BW = f_0/2Q$. As an example, in order to have a bandwidth of 50 Hz, the quality factor should not exceed a value around 200–300.

4) The spring length $L$ and the driving current $I(t)$ can be increased to increase the sensitivity (adjusting accordingly the spring width to cope with point 2 above), in both cases at the cost of an increased power dissipation by Joule effect in the springs $P_{\text{Joule}} \propto I(t)^2 \cdot L$.

Most importantly, the found independence of the sensitivity on the number $N$ of sensing cells is relevant as it suggests the design of an almost 1-D Z-axis MEMS magnetometer that takes up a small area and fits in a lateral side of the same package of the three-axis gyroscope, which shares with the magnetometer the same pressure requirement. While several examples of parallel-plate magnetometers in the literature show a large number of parallel plates [13], a recent device proposed in [20] is based on a small number of sensing cells even if it uses comb fingers and no discussion is presented about the impact of the comb number.

External accelerations or vibrations, typically at frequencies lower than a few kilohertz [23], can be filtered and do not change significantly the operating point of the magnetometer, owing to the high resonance frequency (acceleration-induced displacements are indeed proportional to the inverse of $f_0^2$).

III. DEVICE AND PACKAGING MODEL AND IMPLEMENTATION

The technology used for the device designed in this work is the Thick Epitaxial Layer for Microactuators and Accelerometers surface micromachining process by STMicroelectronics. It is based on the deposition of a sacrificial oxide layer, on which a 15-μm-thick epitaxial layer of polysilicon is first grown, then micromachined through deep reactive ion etching, and finally released through a HF attack. More details on the process can be found in [14]. The packaging is made through a wafer–wafer bonding at a nominal pressure of around 1 mbar. For those devices which require a large quality factor (gyroscopes [18], [19]), a getter material is used within the sealed devices which require a large quality factor (gyroscopes and<br>

<table>
<thead>
<tr>
<th># of plates</th>
<th>$m$ [ng]</th>
<th>$k$ [N/m]</th>
<th>$Q$</th>
<th>$\frac{N_{\text{cell}}}{Q} \frac{\Delta \Phi}{\Delta B/2} \cdot \frac{g_p}{g}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>4</td>
<td>0.56</td>
<td>17.85</td>
<td>481</td>
<td>0.841</td>
</tr>
<tr>
<td>8</td>
<td>0.78</td>
<td>24.50</td>
<td>335</td>
<td>0.843</td>
</tr>
<tr>
<td>16</td>
<td>0.90</td>
<td>28.35</td>
<td>258</td>
<td>0.844</td>
</tr>
<tr>
<td>32</td>
<td>1.80</td>
<td>57.05</td>
<td>223</td>
<td>0.845</td>
</tr>
</tbody>
</table>

we find that the designed magnetometers fall within the free-molecule flow regime ($Kn \approx 35$).

In these working conditions, gas dissipation was computed employing both custom statistical tools, like the test particle Monte Carlo method, and deterministic procedures for the solution of the collision-less Boltzmann model based on integral equations (see [21] and [22]). Since the largest contribution to dissipation comes from local squeeze effects in between the parallel plates, the analysis can be conveniently performed by assuming perfect decoupling between the different plate/stator units. The damping force for a single unit is computed using the algorithm and then multiplied by the number $N$ of shuttle plates. The resulting damping coefficient per unit area turns out to be from the model $b_{\text{area}} = 5.8 \text{ kg} / (s \text{ m}^2)$ at a nominal pressure of 1 mbar.

To discuss possible side effects for this model, one should consider the parallel-plate dimensions. The height $H = 15 \mu m$ is fixed by the process rules. The gap is chosen to be the minimum ($g \approx 2 \mu m$) so that the sensitivity is maximized: Indeed, even if the damping coefficient per unit area $b_{\text{area}}$ is itself an increasing function with respect to $1/g$, on the whole, the sensitivity (which is proportional to the factor $1/(g^2 \cdot b_{\text{area}})$ increases with decreasing $g$. Finally, the sensitivity does not depend on the length of the parallel plates $L_C$ (see the formulas on page 2). Yet, the open-loop bandwidth $(f_0/2Q)$ is a function of the damping coefficient and, in turn, of the length $L_C$. Therefore, we chose to have an overall parallel-plate length of 330 μm, which, from simulations, guarantees a maximum $Q$ of around 450 at the reference pressure (and, thus, a bandwidth of at least > 30 Hz). Therefore, for the sake of simplicity, as the length and the height of each parallel-plate cell are far larger than the gap dimension, the algorithm for the calculation of the damping coefficient neglects side effects. These simulation results have been incorporated in a Simulink behavioral model of the magnetometer. The model accounts for the Lorentz force as well as for the electrostatic readout forces generated during the readout. It lets the user enter the number $N$ of cells and automatically determines the required geometry, mass, and stiffness to keep the resonance frequency to the theoretical value (28.3 kHz in this work). Table I shows the results of this model for four different values of $N$ in terms of mechanical sensitivity and expected quality factor.

To test this theoretical predictions, the four magnetometers have been implemented. Fig. 2 shows a top view of the device having four parallel-plate cells, with its dimensions and the result of a Comsol finite-element method (FEM) simulation of the first resonant mode.
IV. EXPERIMENTAL SETUP AND RESULTS

A. Quality Factor

The mechanical characterization of the designed devices to validate the expected behavior for the quality factor \( Q \) has been performed exploiting the versatile characterization platform proposed in [24]. The value of the quality factor has been estimated from the mechanical response of the structures to downward voltage steps. In every test, the capacitance \( C_1 \) has been used as the electrostatic actuator, applying to it a square voltage waveform sweeping between 0 and 8 V, with a period of 24.5 ms; the capacitance \( C_2 \) has been used as the sensor. Fig. 3 shows examples of the obtained results for the four different structures described in Table I. In every subplot, the variation of \( C_2 \) after the downward voltage step (i.e., toward the rest position) is reported, together with the analytical fitting, from which the depicted resonance frequency and quality factor can be estimated.

For every kind of structure, measurements have been repeated for three different samples. Fig. 4 summarizes the results, showing through circle markers the obtained quality factor as a function of the number \( N \) of differential parallel-plate cells. The light dotted curve is the theoretical prediction from Simulink (including the damping model) described in Section III, at the nominal resonance frequency and the nominal pressure. The dashed curve with cross markers is the result of the same simulation, at the best fitting pressure, which turns out to be a factor \( \approx 1.3 \) higher than expected \([b_{area} = 7.54 \text{ kg/(s}\cdot m^2)]\). The results confirm the theoretical modeling, showing an initial inverse dependence of the quality factor on the number \( N \) of differential sensing cells, justified by the proportionality to \( N \) of the damping coefficient \( b \)

\[
Q = \frac{2 \cdot \pi \cdot f_0 \cdot m}{b} = \frac{2 \cdot \pi \cdot f_0 \cdot (m_{\text{shuttle}} + N \cdot m_{\text{plate}})}{b_{\text{area}} \cdot N \cdot L_{\text{C}} \cdot H}
\]

where \( m_{\text{shuttle}} \) is the mass of the shuttle (which can be neglected only for \( N \gg 1 \)) and \( m_{\text{plate}} \) is the mass associated to every added sensing cell in the structure. Means to electronically enhance the quality factor have been suggested, for instance, in [26] but are not considered here as they require further circuitry at the cost of an increased power dissipation.


**B. Driving and Readout Electronics**

The driving electronics is based on an enhanced Howland current generator (see, e.g., [25] for a description) in order to deliver a precisely known current $I(t)$ to the MEMS. The capacitive readout is based on two transimpedance stages, each made through a low-noise operational amplifier (AD8065 by Analog Devices) with a feedback resistor $R_F = 3 \, \text{M\Omega}$ and a feedback capacitance to set the low-pass pole around 250 kHz (approximately one order of magnitude above the signal at $f_0$). The biasing voltage of the stators with respect to the rotors is set to $V_{\text{BIAS}} = 1.5 \, \text{V}$. The voltage at the output of each amplifier is fed to an instrumentation amplifier (INA129 by Texas Instruments) with a differential gain $G_{\text{INA}} = 50$, whose output voltage turns out to be proportional to the magnetic field $B$.

\[
V_{\text{out,INA}} = G_{\text{INA}} \cdot R_F \cdot V_{\text{BIAS}} \cdot 2\pi f_0 \cdot \frac{\Delta C}{\Delta B} \cdot B.
\]

This signal is then demodulated using a lock-in amplifier (SR830 by Stanford Research Systems) with a further gain of 35. An overall schematic of the electronics used in this work is shown in Fig. 5.

The theoretical minimum measurable signal is determined by the thermomechanical noise of the device, by the noise of the operational amplifiers, and by the noise of each feedback resistance. The main contribution arises from the two feedback resistances, whose overall noise power spectral density is

\[
\sigma_{\text{Resistor}}^2 = 2 \cdot (4 \cdot k_B \cdot T \cdot R) = 9.9 \cdot 10^{-14} \, \text{V}^2/\text{Hz}
\]

where $T$ is the absolute temperature and $k_B$ is the Boltzmann constant; at the output of the lock-in amplifier, the theoretical noise density turns out to be $\sqrt{\sigma_{\text{Resistor}}^2} = 557.2 \, \mu\text{V}/\sqrt{\text{Hz}}$.

**C. Experimental Setup and Results**

The experimental setup hosted in the STMicroelectronics laboratory in Cornaredo is based on the magnetic field compensator and generator *Palm Gauss PG-5G magnetic field canceler* by *Aichi*. This instrument, based on a three-axial system of Helmholtz coils and equipped with reference magnetometers, is capable of precisely measuring the magnetic field in the volume enclosed by the coils and of compensating it. After this calibration, a region of nearly uniform magnetic field of a known value can be generated with a standard deviation of $\approx 80 \, \text{nT/axis}$. The MEMS magnetometer under test, carefully glued and wire bonded to the boards with the driving and readout electronics, is placed at the center of the instrument to avoid side effects due to field nonuniformity near the coils (see Fig. 6).

For each measurement, the magnetometer is driven at its resonance frequency with a peak current of 125 $\mu\text{A/spring}$ (i.e., 250 $\mu\text{A}$ of overall peak current is delivered by the Howland pump). In continuous operation mode, this corresponds to a power dissipated in the springs $P_{\text{inlet}} = 88.3 \, \mu\text{W}$. The instrument first compensates the environmental magnetic field and then generates a stair of increasing field values for the $Z$-axis from $-100$ to $100 \, \mu\text{T}$, by steps of 0.5 $\mu\text{T}$ (keeping nominally null the magnetic field over the other directions). The instrument is controlled through a *Labview* program that also samples, through a data acquisition module (*National Instruments M-6281*), the output value at each point in the ramp. An example of the measurement for the smallest designed device ($N = 4$) is shown in Fig. 7: the voltage output of the acquisition setup is reported on the $y$-axis as a function of the field value on the $x$-axis, together with the best linear fitting after an offset compensation. Offset is typically on the order of $\approx 4 \, \text{mV}$, corresponding to $\approx 27 \, \mu\text{T}$. The obtained overall...
sensitivity is around 150 μV/μT, which is in line with the theoretical predictions of 195 μV/μT for a gap g = 2.1 μm. Differences might result from a process underetch with respect to the expected one. The results obtained for the other devices—having a different number of parallel-plate cells—are similar to the one shown here, in agreement with the modeling theory and the results obtained for the quality factors in Sections II and IV-A. Performance differences on the order of ±5% are observed on devices belonging to different batches; these variations are in line with the measured differences in the resonance frequency, likely due to the process variance obtained in productions specifically done for research purposes.

The resolution was estimated as the standard deviation of a set of 100,000 sampled measurements at the output of the system for an arbitrary fixed value of the magnetic field. Measurements were repeated using different low-pass-filtering bandwidths of the lock-in amplifier in the range 1 Hz–2 kHz. The obtained result is shown in Fig. 8: a comparison with the resistor noise predictions drawn in Section IV-B shows a fairly good agreement for the highest bandwidth. At low frequency values, some more noise can be observed whose origin—still under investigation—may be either in the driving oscillator noise or in the 1/f noise of the lock-in amplifier itself. The obtained results, converted in terms of magnetic field through the measured sensitivity, give a minimum measurable magnetic field of 520 (nT·mA)/(√Hz); this means that a sub-μT/√Hz resolution can be obtained with less than 1 mA of overall driving current.

D. Influence of Possible Fluctuations of the Damping Coefficient

Two main sources of possible variations in the damping coefficient have been considered, to verify their influence on the device mechanical sensitivity, resolution, and bandwidth.

1) Process changes from part to part: For the structures with \( N = 4 \), the measured quality factor at room temperature (about 22 °C) was observed to vary between 335 and 350 for six measured samples. Only a seventh device showed a significantly different \( Q \) (around 372).

2) Changes of the damping coefficient with temperature: This phenomenon, related to the changes of the pressure with temperature within a constant volume package, is well described, for instance, in [27]. In this reference, Kim suggests a dependence of the quality factor on the temperature with a \( 1/T^\gamma \) law, with \( \gamma \) being close to 0.5 in the case of air damping. Measurements of the quality factor with respect to temperature [24], [28] were performed in this work using the \( MN/TY55 \) climatic chamber from Angelantoni Climatic Systems, to verify the temperature behavior in the presented devices.

The results are shown in Fig. 9. In our experiments, the quality factor shows a variation from 340 to 312 for a measurement range between 5 °C and 45 °C. It can be thus evidenced a percentage variation of the quality factor around 0.2%/K. Within this range, the measured device resonance frequency varies less than 20 ppm/K, confirming that the changes in \( Q \) are mostly related to changes in pressure.
The problem of out-of-plane assembling of the Z-axis AMR magnetometer results in a 7-DOF MEMS IMU that solves the problem of out-of-plane assembling of the Z-axis AMR magnetometer.

Considering the typical operation range of consumer devices (−40 °C to 85 °C), it turns out that, among the two possible sources of fluctuation of the damping coefficient, the most relevant one is its dependence on the temperature (25% change on the whole range). When discussing large-volume applications, one should however consider that all the parts are generally subject to an initial calibration: this compensates for the native changes from part to part due to the process variance. One should moreover take into account that a temperature sensor is often embedded in the same application-specific integrated circuit which includes the MEMS readout electronics: This can be exploited in operation for postacquisition compensation of temperature changes. Therefore, initial calibration and temperature compensations similar to those applied, for instance, to a gyroscope (such a device is based too on resonant driving and sensing) will be mandatorily required also for the magnetometers. Note that these postproduction operations correct digitally for the overall device gain; issues that still remain unchanged, even after the suggested compensations, are the change in the intrinsic resolution (due to the change in the native mechanical sensitivity) and the change in the open-loop bandwidth (due to the change in the quality factor). The former degrades at high temperatures, while the latter degrades at low temperatures.

V. CONCLUSION

This paper has demonstrated that Z-axis magnetometers operating in industrial packages can be designed in very compact dimensions with performance suitable for the requirements needed by consumer applications, if suitably compensated for temperature changes. Some further improvements can be obtained, such as the following: 1) by increasing the spring length up to 1100 μm (after discussing with process engineers, this has been found to be the maximum suspended dimension allowed presently by the technology without relevant residual stresses); 2) by decreasing the resonance frequency to the limits of the acoustic bandwidth; and 3) by increasing the biasing voltage $V_{BIAS}$ to 3 V. Combining these changes, a 3.84× increase in sensitivity and resolution can be obtained. Owing to its high aspect ratio, such a device still fits laterally in the same package as the gyroscope (see the schematic sketch in Fig. 10). The suggested combination can soon lead to an all-MEMS 7-DOF IMU, solving the problem that AMR magnetometer is not sensitive to out-of-plane magnetic fields.

Concerning dissipation, it can be observed that, in MEMS magnetometers, given a maximum available power, there is a tradeoff between the dissipated power by Joule effect in the springs and by the very large scale of integration (VLSI) electronics. The former contribution could be minimized if the resistivity of the springs were lowered, for instance, by depositing metal paths on the suspended beams. This feature might become available with the technological progress. Lowering this dissipative term, and increasing the sensitivity as described, would allow one to design integrated electronics with a lower resistance $R_P$ and a larger available power dissipation, to obtain better noise performance. The design and test of a VLSI readout circuit for this kind of magnetometer, taking into account the given results and the said considerations about power dissipation, are in progress.

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