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**A THEORETICAL AND EXPERIMENTAL STUDY OF
THE VISCOUS DAMPING EFFECT FOR DIAPHRAGM-
BASED MEMS APPLICATIONS**

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Abstract

In the analysis and design of microelectromechanical system (MEMS) devices, volume and surface forces are two physical quantities that are commonly encountered. During miniaturization, equal reduction of both the volume and the surface of a microstructure is not normally achievable. A 10% reduction in length results in a 1000-time reduction in the volume but only a 100-time reduction in the surface area. Therefore, the surface force effect of viscous damping has a more significant influence on the mechanical performance of MEMS devices such as frequency response characteristics, mechanical-thermal noise and switching speed.

Simple mechanical resistance expressions, such as those formulated by Škvor/Starr, are commonly used to describe the mechanical performance of a condenser (or capacitive) microphone without considering the location effect of acoustic holes in the backplate on both the frequency response characteristics and the mechanical-thermal noise. On the other hand, a more accurate, but more complicated, comprehensive modeling approach has been described by Zuckerwar, which has the capability to describe such a hole location effect. A 2^6 factorial design, which consists of six backplate parameters and four responses, is carried out on Zuckerwar's approach. Through this design of experiment (DOE) technique, a valuable perspective into the backplate design optimization is acquired.

The Škvor/Starr approach does not consider the location effect of holes and overestimates the mechanical-thermal noise. A modified Škvor/Starr approach is proposed to address this inadequacy, in which a mode shape factor, which consists of the zero order Bessel and modified Bessel functions, is included in Škvor's mechanical

resistance formulation to account for the hole location effect. With reference to two B&K microphones (0.5-inch (Type 4134) and MEMS technology), the theoretical results of the *A*-weighted mechanical-thermal noise obtained by the modified Škvor/Starr approach are in good agreements with those reported experimental ones.

Taking the developed backplate design optimization strategy a step further, it is demonstrated experimentally, via a PZT/Si diaphragm-based acoustic transducer, that its frequency response characteristics can be optimized by attaching a silicon backplate with optimized slots and holes. The attached backplate increases the damping effect by introducing viscous damping primarily in the air gap, slots and holes. The bandwidth of the acoustic transducer is approximately 18 kHz with an estimated *A*-weighted mechanical-thermal noise of 28.6 dB(A).

For the radio frequency (RF) MEMS shunt switch, an optimized air gap and strategically-positioned holes in the base substrate greatly enhance its switching speed. The increase in the switching speed can be attributed to the significant reduction of the squeeze-film damping effect as a result of the hole implementation. The pull-in and release times of the RF switch are significantly reduced from 5.40 ms to 0.44 ms and 40.60 ms to 3.20 ms, respectively, when comparisons are made with one without any perforation scheme. This represents a switching time reduction of more than 12 times.

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List of Symbols

Alphabets A to Z

a	diaphragm radius, m
a_k	location of k th acoustic hole in backplate, m
a_r	location of acoustic holes in backplate, m
a_l	location of acoustic slot in backplate, m
A	area of diaphragm, m^2
	fraction of open area in backplate
A_c	capacitive signal attenuation due to C_i and C_s
A_v	preamplifier gain
b	backplate radius, m
b_c	width of the top corrugation, m
b_k	number of acoustic holes per radius ring
B_a	damping coefficient, Ns/m
c_T	isothermal sound speed in air, 290.2 m/s
C_A	air layer compliance, m^5/N
C_i	preamplifier input capacitance, F
C_M	membrane compliance, m^5/N
C_{mic}	microphone capacitance, F
C_s	stray capacitance, F
C_{to}	static capacitance of microphone, F
d	diaphragm thickness, m
D	air layer compliance and damping term

E	Young's modulus, N/m ²
E_o	polarization voltage, V
f	frequency, Hz
f_k	velocity at k th acoustic hole in backplate, m/s
$f(r, \theta)$	velocity distribution function at backplate, m/s
h	unpolarized air gap, m
h_c	depth of corrugation, m
h_o	polarized air gap at center, m
k_B	Boltzmann constant, 1.38×10^{-23} J/K
K	wave number of diaphragm, m ⁻¹
l	characteristic length of plate, m
l_k	depth of k th acoustic hole in backplate, m
	thickness of backplate wafer, m
l_l	depth of acoustic slot in backplate, m
M	overall sensitivity of microphone, V/Pa
M_e	electrical sensitivity of microphone, V/m
M_m	mechanical sensitivity of microphone, m/Pa
M_M	membrane mass, kg/m ⁴
M_{oc}	open-circuit sensitivity of microphone, V/Pa
m	number of summation terms
N	number of acoustic holes in backplate
N_c	number of corrugations
N_f	noise voltage spectral density of mechanical-thermal noise, V Hz ^{-1/2}

N_T	A -weighted mechanical-thermal noise, dB(A)
p	reaction pressure in air layer, Pa
p_i	incident acoustic pressure, Pa
p_k	pressure at k th acoustic hole in backplate, Pa
$p(r, \theta)$	pressure distribution in air layer, Pa
p_l	pressure at acoustic slot in backplate, Pa
P_a	ambient pressure, Pa
P_f	pressure spectral density of mechanical-thermal noise, Pa Hz ^{-1/2}
r_e	equivalent radius of new collecting diaphragm, m
r_k	radius of k th acoustic hole in backplate, m
r_l	width of acoustic slot in backplate, m
R_A	air layer damping resistance, Ns/m ⁵
R_{airgap}	equivalent mechanical resistance due to squeeze-film damping, Ns/m ⁵
R_{perf}	equivalent mechanical resistance due to acoustic holes, Ns/m ⁵
R_{lhole}	equivalent mechanical resistance due to a single hole, Ns/m ⁵
s_k	area of k th acoustic hole in backplate, m ²
S_{bp}	backplate area, m ²
S_{dia}	diaphragm area, m ²
$t_{pull-in}$	pull-in time, s
$t_{release}$	release time, s
T	diaphragm tension, N/m
	absolute temperature, K
T_m	see Chapter 2, Eq. (14) [Wong and Embleton 1995]

U	volume velocity, m^3/s
U_k	volume velocity at k th acoustic hole in backplate, m^3/s
U_l	volume velocity at acoustic slot in backplate, m^3/s
v	air particle velocity, m/s
V	backchamber volume, m^3 actuation voltage, V
V_{in}	pull-in voltage, V
V_{pi}	pull-in voltage, V
V_C	cavity volume, m^3
w_c	width of the bottom corrugation, m
$W(a_r)$	mode shape factor
y_m	function, see Chapter 2, Eq. (14) [Wong and Embleton 1995], m
y_0	unknown displacement in trial expression, see Chapter 2, Eq. (14) [Wong and Embleton 1995], m
$y(r, \theta)$	diaphragm displacement, time-varying distribution, m
Z_C	acoustic impedance of backchamber, Ns/m^5
Z_k	acoustic impedance of k th acoustic hole in backplate, Ns/m^5
Z_m	acoustic impedance of the diaphragm-air layer system, Ns/m^5
Z_l	acoustic impedance of acoustic slot in backplate, Ns/m^5

Greek

β	profile angle of the corrugation, degree
γ	specific heat ratio of air, 1.403 slip-flow constant
Γ_m	see Chapter 2, Eq. (14) [Wong and Embleton 1995]

$\delta(m) = 1$ if $m = 0$
 $= 0$ if $m \neq 0$ see Chapter 2, Eq. (14) [Wong and Embleton 1995]

ϵ_0 dielectric permittivity of free air, 8.85×10^{-12} F/m

ξ_m radial component of k , symmetric, m^{-1}

η residual stress reduction ratio

λ mean free path of gas molecules, m

ρ time-varying air density, kg/m^3

ρ_0 density of static air, 1.205 kg/m^3

σ_M diaphragm mass surface density, kg/m^2

σ squeeze number

equilibrium diaphragm stress with corrugations, Pa

σ_0 diaphragm initial stress, Pa

σ_r diaphragm residual stress, Pa

μ absolute viscosity of air, $17.9 \times 10^{-6} \text{ Ns/m}^2$

ν Poisson's ratio

ω angular frequency, rad/s

Matrices

P pressure matrix

U volume velocity matrix

Z acoustic impedance matrix

Functions and Functional Symbols

$A()$ function of A -weighted filter

$\text{Im}()$ imaginary part

$j = (-1)^{1/2}$	imaginary part
$I_0()$	cylindrical zero order modified Bessel function of the first kind
$J_0()$	cylindrical zero order Bessel function of the first kind
$J_1()$	cylindrical first order Bessel function of the first kind
$J_2()$	cylindrical second order Bessel function of the first kind
$\text{Re}()$	real part

Numerical Subscripts

$k, s = 1, 2, \dots, q$
$m = 0, 1, \dots, \infty$

Abbreviations

BW	bandwidth, Hz
SNR	signal-to-noise ratio, dB
SPL	sound pressure level, dB

Chapter 1

Introduction

1.1 Overview of MEMS Devices and Microsystems

The advent of silicon micromachining technology has opened up numerous opportunities for the commercialization of batch-processed, high-performance miniaturized devices [1]. Matured silicon microfabrication technology has its origin in the integrated circuit (IC) fabrication technology that is used in the microelectronics industry. Among these microfabrication techniques are photolithography, oxidation, doping, thin-film deposition and etching processes. Three-dimensional microstructures, such as holes, pits, recesses, cantilever beams, diaphragms and comb-drives, can be fabricated on the silicon substrates by using the etching techniques. For thin-film deposition techniques, successive layers of materials can be built on the silicon substrates for functional purposes such as electrical and thermal insulation, dielectric layers, electrical ohmic contacts, electrical interconnection and solder pads.

The specific and desired attraction of a miniaturized device is simply its size since a small-size device allows for a much wider area of applications than a bigger one. For the technology savvy consumers, there is an ever-growing demand for smaller and better performance portable gadgets to meet the vigorous requirements of their daily lifestyles. For the science and technology folks, miniaturized sensors are more sensitive, require less measurand sample, consume less power and less intrusive to the measurand environment, which ensure a more accurate and reliable sensing operation. To the manufacturers, the miniaturization technology readily lends itself to cost-

sensitive production environment, where worldwide outsourcing of discrete components competes and dominates on a cost basis, and mass production capability. The realization of the mass production of miniaturized devices on relatively inexpensive silicon wafers represents an enormous amount of cost-saving potential to manufacturers as a significant higher number of miniaturized devices can be fabricated on silicon wafers than in the conventional manufacturing methods.

The key elements in MEMS devices can be classified into two main components, namely a sensing or an actuating part and a transducing unit. For MEMS sensors, their primary task is to survey the environment, in which they are deployed, and to provide a quantitative indication of the intensity of the environmental parameters that they are designed to sense. These environmental parameters can widely range from physical quantities, such as pressure, temperature and light, to chemical and biological ones such as chemical and virus composition. An incorporated transduction unit is then required to translate the intensity changes of these environmental parameters into a tangible voltage output signal. For example, in a piezoresistive pressure sensor, incoming pressure variations are picked up by an elastic thin silicon diaphragm. The resulting diaphragm deflection induces an electrical resistance change in the piezoresistors, which are implanted close to the diaphragm edge. With the help of a Wheatstone bridge circuit as part of the transduction unit, the resultant resistance change is converted into the corresponding voltage change.

For MEMS actuators, their primary responsibility is to influence the environment, in which they are deployed. The transduction unit converts the input power supply or signal into physical actuating forces, such as electrostatic force

(voltage), thermal force (resistance heating by current), piezoelectric effect (voltage) and magnetic force (current), to create minute movement of microstructures such as a cantilever, diaphragm and comb-drive. For example, one popular actuation mechanism is the electrostatic actuation, in which the actuating microstructure resembles that of an air capacitor. By the application of an actuating voltage to the electrode plates, the electrostatic forces generated by the charged parallel plates, which are separated by a thin dielectric layer of air, promote the relative movement of these plates in the normal direction of aligned plates or parallel movement for misaligned plates.

A microsystem is a complete MEMS engineering solution that incorporates some forms of intelligence to carry out some specific functions. A microsystem can be thought of consisting of MEMS sensors and actuators, transduction units and a processing unit packaged together in a system level on the same IC chip. In a typical scenario, the environmental data received by a sensor are converted into a compatible actuating signal for the actuator via the transduction units. Upon analyzing the data signal, the processing unit will subsequently proceed to make a decision whether or not to trigger the actuator into performing a pre-determined set of actions. Most microsystems are designed and engineered in a way to carry out a specific single function. For example, in an airbag microsystem, two microaccelerometers, which are deployed in both the horizontal and the vertical orientations, sense the shock impact of a car in a collision. After that, the sensors generate and transmit an actuating signal via transduction and processing units to the actuator, which then activates the airbag deployment system in the car to protect the driver and passengers from injuries.

As newer MEMS devices and microsystem products have become available to

users, the market demand for these products has been expanding very rapidly over the years. Tables 1.I and 1.II illustrate the world market projections for the microsystem products to the year 2000. From Table 1.I, it is clear that the applications of microsystems in the aerospace, automotive, biotechnology, consumer products, defense, environmental protection and safety, healthcare, pharmaceutical and telecommunications industries led to many industry experts to account for a staggering \$82 billion in revenue for the microsystems and related products in the year 2000. The compound annual growth rate since 1992 is about 15.9% while the unit prices continue to drop every year. The pressure sensor is among the first few microsystem products to be successfully commercialized and part of the reason can be attributed to the boom in the automotive industry during that period of time. With newer microsystem products entering the market each year, the pressure sensor market share is expected to drop in the years to come.

Both biomedical sensors and biosensors are projected to garner very significant worldwide market share for microsystem products in the near future. Biomedical sensors are primarily used for clinical diagnostic analyses. As a result of their miniaturized size, these sensors are highly sensitive because only a minute amount of biological sample is needed and analyzed results can be made available significantly faster than their traditional bulky counterparts in biomedical instruments. Significant cost-saving to the practitioners is another attractive aspect of these biomedical sensors. Firstly, these sensors are manufactured in batches, which can help to bring down their unit cost. Secondly, because these sensors are designed to be disposable after usage, there are no additional costs such as employing manual labour to do the cleaning and

utilizing cleaning equipments for each subsequent reuse. Biosensors, on the other hand, are commonly used in analytical chemistry and biomedicine applications as well as genetic engineering.

Table 1.I Worldwide silicon-based microsensor market.

Year	Units, 1000	Revenues, million \$	Ave. unit price, \$	Revenue growth rate, %
1989	3026	570.50	188.53	—
1990	5741	744.60	129.70	30.5
1991	6844	851.70	124.44	14.4
1992	7760	925.40	119.25	8.6
1993	8816	977.10	110.83	5.6
1994	10836	1116.20	103.00	14.2
1995	13980	1316.30	94.16	17.9
1996	18127	1564.40	86.30	18.9
1997	23514	1857.60	79.00	18.7
1998	30355	2199.80	72.47	18.4
1999	38792	2593.80	66.86	17.9

Principal reference: Frost & Sullivan market intelligence, 1993.

Table 1.II World market projection [2] for MEMS and sensors (in millions of dollars).

	1995	1997	1999	2000
Si sensors in all applications	1316 ¹	1858	2549	
Si sensors in automobile	414	621	884	
Si sensors & Si microsystems				3,665
Si-based MEMS ²	2700			11,900 ³
All sensors	8500			13,100
All industries and scientific instruments	5900			82,000
Grand-Total:				110,665

Note: 1. United States: 54.4%; Europe: 24.4%; Pacific Rim: 21.2%.

2. Pressure, gas, chemical and rate sensors, accelerometers, valves and actuators, nozzles, displays, microrelays.

3. \$5.5 billion in sensors; \$2.3 billion in microstructures; \$2.6 billion in microsystems; \$1.5 billion in microactuators (40% for automobiles).

Principal reference: *Fundamentals of Microfabrication*, Marc Madou, CRC Press, 1997.

1.2 Viscous Damping of MEMS Devices

In the analysis and design of MEMS devices, volume and surface forces are the two physical quantities that are commonly encountered. The volume force originates from the mass of the device components, such as the proof mass of an accelerometer and the diaphragm of a microphone, and it is a measure of the mechanical inertia of the devices. On the other hand, the surface force is closely associated with the physical phenomenon that originates from the surface interaction between the device components and their surrounding environment. Examples of such a surface phenomenon include the viscous force and the spring force. During miniaturization, the equal reduction of both the volume and the surface of a microstructure is not normally achievable [3]. The volume force has a volume $\propto$ length³ relationship while that of the surface force is surface $\propto$ length². A 10% reduction in length results in a 1000-time reduction in the volume (mass as well) but only a 100-time reduction in the surface area. As a result, the influence of the surface force on a microstructure is greater than that of the volume force. Thus, the surface force effect of viscous damping has a significant effect on the mechanical performance of MEMS devices such as the frequency response characteristics [4], mechanical-thermal noise [5] and switching speed [6]. The minute movement of miniaturized mechanical components is adversely affected by the surrounding air medium, which manifests itself as a reaction force that opposes the motion of the moving components. The viscous damping effect becomes more and more pronounced as the microstructure gets smaller and smaller.

The introduction of the silicon micromachining technology to the fabrication of microstructures enables the creation of a very thin air gap, which can provide sufficient

damping through the viscous dissipation in the air medium. If the thickness of the air film is smaller than one-third of the width of the enclosed plates, which is usually the case for MEMS devices having such structures, the squeeze-film damping [7, 8] is the most dominant damping effect for transversely driven planar devices. As such, during the design of capacitive sensors and electrostatic actuators, the squeeze-film damping effect must be considered. In many practical cases, only the low frequency behavior of such transducers is of interest and hence, only the viscous damping effect needs to be accounted for. As the air film is not appreciably compressed in these cases, the compressibility effect of the air film (i.e. spring force) can be ignored. For capacitive sensors, the squeeze-film damping effect manifests itself in both the frequency response characteristics (sensitivity and bandwidth) and the mechanical-thermal noise. For electrostatic actuators, the squeeze-film damping effect shows up in the dynamic response characteristics (displacement and actuation speed). For laterally driven planar microstructures, in which two parallel plates are in relative tangential motion, the slide-film damping effect [9] develops in the fluid layer enclosed by the plates. As the thickness of this fluid layer decreases, the slide-film damping effect becomes a dominant source of energy dissipation in the laterally driven planar devices.

1.3 Motivations

Although the basic characteristics of the squeeze-film air damping effect have been fully understood by 1980s, the advent of MEMS technology has continued to fuel research activities in this important field. For example, the recently developed deep reactive ion etching (DRIE) technique enables the fabrication of MEMS devices with

thick perforated plate. As a result, an adequate model for the squeeze-film damping of the perforated plate is necessary and highly desirable.

Despite the size advantage, one major challenge during the design of MEMS devices involves the optimization of the damping characteristics of the mechanical structures. For miniaturized structures, viscous damping dominates the dissipation mechanism, which can have an adverse effect on the mechanical performance of MEMS devices. Depending on the types of applications, the introduction or the deficiency of damping plays an important role in determining whether the devices are capable of fulfilling the desired performance specifications. As such, it is very crucial to develop a basic understanding of the viscous damping effect so that MEMS designers can use it to their advantage to come up with better performance devices. In this perspective, a theoretical and experimental study of the viscous damping effect is carried out for diaphragm-based MEMS applications.

1.4 Objectives and Scope

The main objectives and scope of the project work are as follows:

- For two parallel plates, the squeeze-film damping effect [7, 8], which can have a significant influence on the mechanical performance of MEMS devices, dominates the dissipation mechanism for an air gap of several microns. When holes are perforated in one of the plates, this viscous damping effect can be reduced significantly as air has an alternative path to escape through the holes rather than being squeezed out from the edge of the plate. As such, one of the main objectives is to carry out a detailed study of the viscous damping effect for

diaphragm-based MEMS applications. Subsequently, with the acquired knowledge, it can be exploited to design and fabricate better performance and more reliable diaphragm-based MEMS devices.

- Zuckerwar [10, 11] developed an accurate, but complicated, comprehensive modeling approach, which has the ability to address the hole location effect on the performance of a condenser microphone. As such, a 2^6 factorial design, which consists of six backplate parameters (air gap, absence or presence of a slot, hole location, hole number, hole radius and backplate thickness) and four responses (open-circuit sensitivity, A -weighted mechanical-thermal noise, bandwidth and C_A/C_M ratio) is carried out on Zuckerwar's approach to acquire a valuable insight into the backplate design optimization process.
- Simple mechanical resistance expressions, such as those formulated by Škvor/Starr [5], have been very popular among many authors [12–17] despite their inherent limitations. The Škvor/Starr approach, which does not consider the location effect of holes in the backplate, overestimates the mechanical-thermal noise. As such, a modified form of the Škvor/Starr approach is proposed to address this hole location dependent effect and verified against two B&K microphones. A mode shape factor, which consists of the zero order Bessel and modified Bessel functions, is included in Škvor's mechanical resistance formulation to account for the effect of the hole location.
- With the developed backplate design optimization strategy, it is applied to the design and fabrication of two diaphragm-based MEMS devices: (1) PZT/Si acoustic transducer and (2) RF MEMS shunt switch. For the PZT/Si acoustic

transducer, a silicon backplate, with optimized slots and holes, optimizes its bandwidth over the audio frequency range for both the microphone and speaker configurations. For the RF MEMS shunt switch, a silicon base substrate, with an optimized air gap and strategically-positioned holes, greatly enhances its switching speed by a factor of more than 12 times.

1.5 Thesis Organization

The thesis is organized into seven main chapters. Chapter 1 begins with an overview of MEMS devices and microsystems, and offers a brief discussion on the effect of viscous damping on the mechanical performance of miniaturized devices. It also spells out clearly the motivation behind the research work.

Chapter 2 presents a concise literature review on squeeze-film damping and its important influence on the performance characteristics of MEMS devices. A critique of the existing literature is also summarized while solutions are proposed to overcome these limitations.

Chapter 3 is divided into two main sections. The first section provides a very brief theoretical review of the condenser microphone theory and a MATLAB program is written to implement it, which is then validated against a B&K condenser microphone. The latter section covers a detailed backplate parameter study of the condenser microphone via the DOE perspective. The results from this parameter study are then applied to the design of a PZT/Si diaphragm-based acoustic transducer and a RF MEMS shunt switch in Chapter 5 and 6, respectively.

Chapter 4 deals with the modified Škvor/Starr approach, which accounts for the

location effect of holes in the backplate, in the mechanical resistance analysis of the condenser microphone. The theoretical results are then verified against the experimental data of two B&K condenser microphones.

Chapter 5 documents the successful demonstration of a PZT/Si diaphragm-based acoustic transducer, in which its frequency response characteristics can be desirably modified by increasing the viscous damping effect via the attachment of a silicon backplate to the structure of the transducer without any drastic increase in its mechanical-thermal noise.

Chapter 6 details the successful demonstration of a RF MEMS shunt switch, in which its switching speed can be enhanced by a factor of more than 12 by decreasing the viscous damping effect via the implementation of through-holes in the base substrate.

Finally, Chapter 7 provides a brief summary of the accomplished work in the thesis, highlights the major contributions and suggests some future work.

1.6 Conclusion

An overview of MEMS devices and microsystems has been briefly discussed, highlighting the effect of viscous damping on the mechanical performance of MEMS devices. The motivations behind the research work and the scope of the thesis have also been clearly defined.

References

- [1] K. E. Petersen, "Silicon as a mechanical material," *Proc. IEEE*, vol. 70, pp. 420–457 (1982).
- [2] M. Madou, *Fundamentals of Microfabrication*, CRC Press, Boca Raton, 1997.
- [3] T. R. Hsu, *MEMS & Microsystems: Design and Manufacture*, McGraw-Hill, New York, 2002.
- [4] T. Tschan, N. de Rooij and A. Bezing, "Damping of piezoresistive silicon accelerometers," *Sens. Actuators A*, vol. 32, pp. 375–379 (1992).
- [5] T. B. Gabrielson, "Mechanical-thermal noise in micromachined acoustic and vibration sensors," *IEEE Trans. Electron Devices*, vol. 40, pp. 903–909 (1993).
- [6] P. G. Steeneken *et al.*, "Dynamics and squeeze film gas damping of a capacitive RF MEMS switch," *J. Micromech. Microeng.*, vol. 15, pp. 176–184 (2005).
- [7] J. B. Starr, "Squeeze-film damping in solid-state accelerometers," *IEEE Workshop in Solid-state Sensor and Actuator 4th Technical Digest*, pp. 44–47 (1990).
- [8] M. Bao and H. Yang, "Squeeze film air damping in MEMS," *Sens. Actuators A*, vol. 136, pp. 3–27 (2007).
- [9] Y. H. Cho, B. M. Kwak, A. P. Pisano and R. T. Howe, "Slide film damping in laterally driven microstructures," *Sens. Actuators A*, vol. 40, pp. 31–39 (1994).
- [10] A. J. Zuckerwar, "Theoretical response of condenser microphones," *J. Acoust. Soc. Am.*, vol. 64, pp. 1278–1285 (1978).
- [11] A. J. Zuckerwar, "Principles of operation of condenser microphones," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.
- [12] P. R. Scheeper, A. G. H. van der Donk, W. Olthuis and P. Bergveld, "Fabrication of silicon condenser microphones using single wafer technology," *J. Microelectromech. Syst.*, vol. 1, pp. 147–154 (1992).
- [13] W. Kühnel and G. Hess, "A silicon condenser microphone with structured backplate and silicon nitride membrane," *Sens. Actuators A*, vol. 30, pp. 251–258 (1992).

- [14] P. R. Scheeper, W. Olthuis and P. Bergveld, "Improvement of the performance of microphones with a silicon nitride diaphragm and backplate," *Sens. Actuators A*, vol. 40, pp. 179–186 (1994).
- [15] M. Pedersen, W. Olthuis and P. Bergveld, "A silicon condenser microphone with polyimide diaphragm and backplate," *Sens. Actuators A*, vol. 63, pp. 97–104 (1997).
- [16] M. Pedersen, W. Olthuis and P. Bergveld, "High-performance condenser microphone with fully integrated CMOS amplifier and DC-DC voltage converter," *J. Microelectromech. Syst.*, vol. 7, pp. 387–394 (1998).
- [17] R. Nadal-Guardia, A. M. Brosa and A. Dehé, "AC transfer function of electrostatic capacitive sensors based on the 1-D equivalent model: Application to silicon microphones," *J. Microelectromech. Syst.*, vol. 12, pp. 972–978 (2003).

Chapter 2

Literature Review

2.1 Introduction

The simplest mechanical vibration system is the mass-spring system and its natural frequency is dictated by the ratio of spring constant to mass. The natural frequency is a very important performance evaluation criterion of transducers as it provides an insight into the resonant magnitude at the resonant frequency. For the free vibration of a mass-spring system, the system response will be oscillatory in nature with constant amplitude. If a dashpot (or a damper) is introduced into the mass-spring system, it will induce a mechanical resistance (or a damping effect) to the motion of the vibrating mass, which results in the reduction of the vibrating amplitude. The introduction of the dashpot generates an opposing damping force, which is proportional to the velocity of the vibrating mass, that tends to lower the resonant frequency.

Depending on the amount of damping in the system, three scenarios (from high to low damping) are possible as illustrated in Fig. 2.1: (1) Overdamped, (2) critically damped and (3) underdamped responses. In the case of overdamped response, the vibration amplitude of the mass decreases very rapidly. Its frequency response is characterized by a flat response with no resonance and a narrow bandwidth that is due to damping losses at low frequency. Thus, the overdamped response is coveted in systems that are vulnerable to excessive vibration. For critically damped response, the vibration amplitude of the mass decays slowly without the occurrence of any oscillatory motion. Its frequency response is characterized by a flat response with no

Fig. 2.1. Frequency response characteristics of overdamped, critically damped and underdamped systems.

resonance and a wider bandwidth than that of the overdamped system as the damping losses occur at higher frequency. Therefore, the critically damped response is desirable in systems that require a wider bandwidth and can tolerate minor vibration. For underdamped response, the mass remains in oscillatory motion for a long time although the amplitude of vibration decays continuously. Its frequency response is characterized by a flat response before the occurrence of the resonant frequency. As a result, the underdamped response is suitable for systems that require a larger amplitude (i.e. a larger output signal) of mass vibration at the resonant frequency.

In addition to influencing the frequency response characteristics [1], the damping effect also exerts its influence on the mechanical-thermal noise [2] performance of small-sensor systems and the switching speed [3] of micromechanical switches. Silicon micromachining enables the fabrication of a very thin air gap that provides an excellent channel of viscous dissipation, even when the fluid medium is air. This eliminates the usage of higher viscosity liquid fluids to achieve the required

damping for the mechanical structures. Moreover, the damping effect becomes less temperature dependent due to the smaller relative viscosity change in air when compared to a liquid. Thus, the presence of damping or the lack of it in microstructures is a very important design consideration for MEMS designers so that performance characteristics of MEMS devices can be tailored to desired specifications.

In addition to the widely used mechanical dampers for pneumatic machines, an air film also plays an important role in the air film lubrication in applications such as air bearings and levitation systems. Hence, before the introduction of MEMS technology, an extensive knowledge of the squeeze-film damping effect, with regards to the air film lubrication, has already been available in literatures. Most of the literatures are devoted to the parallel movement of the surfaces rather than the normal one. The significance of the squeeze-film damping has not gone unnoticed and many detailed studies have been carried out to investigate its effect on small movable mechanical components.

For example, in the design of high sensitivity accelerometers [1, 4, 5], a resonant vibration of the proof mass is desired because the resultant large vibration amplitude of the mass provides a large output signal. Beyond the resonant frequency, the vibration amplitude of the mass is very small. This input/output characteristic can be realized by designing a mechanical system with a very low level of damping. Usually, the resonance phenomenon, which can have a catastrophic effect on the mechanical structure, is avoided by increasing the natural frequencies of the structure such that all conceivable frequencies induced by the applied excitation force will not

even reach the lowest of all modes of the natural frequencies. In the case of accelerometers, exceptions are made, in which the occurrence of resonance is desired.

In the case of condenser microphones [6–22], the damping effect is introduced into the mechanical system by the backplate electrode. As a result of the two-chip nature, they suffer from air gap losses due to a higher level of mechanical damping. Typically, an optimum level of damping is required to achieve the desired performance characteristics. A high damping value results in a lower microphone sensitivity, narrower bandwidth and higher mechanical-thermal noise while a low damping value provides a higher sensitivity, wider bandwidth with the presence of resonance and lower mechanical-thermal noise. An optimized damping value can be achieved by the meticulous design of the backplate structure (air gap, slot and holes).

In the case of piezoelectric-based acoustic transducers [6–8, 23–29] and piezoresistive microphone [30], a strong resonance can be observed in the frequency response characteristics, which can be attributed to the mechanical structures behaving like a mass-spring system with a very low level of damping. This resonance phenomenon is a typical characteristic of one-chip transducers, which can adversely reduce the bandwidth of the transducers. Very often, the very low level of damping is provided by the packaging that houses the transducers [28]. Being one-chip transducers, they do not suffer from air gap losses as compared to condenser ones, where it is necessary to incorporate another chip to form a pair of electrodes that sandwiches an air gap.

For RF MEMS switches [31–43], arrays of through-holes can be implemented in the suspended bridge/diaphragm structures to reduce the squeeze-film damping effect so that the switching speed of the RF switches can be enhanced greatly [35, 39].

2.2 Review of Squeeze-film Damping

Blech [44] first broached the idea of using the squeeze-film damping effect to tailor the frequency response characteristics of seismic accelerators. Thereafter, the squeeze-film damping effect becomes an important design consideration in the performance specifications of MEMS devices such as microaccelerators, microphones, microspeakers, micromechanical switches, ultrasonic transducers and etc.

In general, the squeeze-film damping effect is governed by both the viscous (surface area) and inertia (volume) effects within the fluid. However, for most MEMS devices, the viscous damping effect dominates the dissipation mechanism in the mechanical system while the inertia effect, which becomes prominent only at very high frequencies, is very often negligible. As a result, the relative movement in the lateral direction is not considered. In such case, the squeeze-film behavior of the fluid can be governed by the well-known Reynolds equation [45]. Also, at very small dimensions, the temperature variations within a miniaturized device are usually ignored.

For two parallel plates, the opposing resistive force acting on the actuating plate, which moves normally against the stationary plate, is a result of the damping pressure between the two plates. The damping pressure comprises of two main components: (1) the pressure component that causes the viscous flow of air when it is being squeezed out or sucked into both plates and (2) the pressure component that

causes the compression of the air film. The force component that corresponds to the viscous air flow is known as the viscous damping force, which is proportional to the velocity of the actuating plate. The force component that corresponds to the air film compression is known as the elastic damping force (or spring force), which is proportional to the displacement of the actuating plate.

If the actuating plate is oscillating at a low frequency (i.e. low velocity), the sandwiched air film is not compressed significantly. In this scenario, the viscous damping effect becomes dominant and the elastic damping effect can be neglected. Therefore, this case corresponds to the “incompressible gas” condition. Examples of MEMS devices that fall into this low frequency application category include microaccelerators, microphones, microspeakers and micromechanical switches.

However, on the other hand, if the actuating plate is oscillating at a high frequency (i.e. high velocity), the sandwiched air film is compressed but it cannot escape from the edges of the plates. In this particular case, the elastic damping becomes the dominant effect over the viscous damping and it is known as the “compressible gas” condition. An example of a MEMS device that can be categorized into this high frequency regime is the ultrasonic transducer.

2.2.1 Frequency Response Characteristics

From the above description, it is very obvious that the squeeze-film damping effect contributes additional viscous and elastic damping forces to the vibration system. Hence, the effect of the squeeze-film damping on the dynamic behavior of a mass-spring system can be significant and must be taken into consideration. The squeeze-

film damping effect is strongly dependent on the ratio of the natural frequency (ω_n) to the cut-off frequency (ω_c) [45], with which three scenarios are possible: (1) $\omega_n \ll \omega_c$, (2) $\omega_n \approx \omega_c$ and (3) $\omega_n \gg \omega_c$.

Fortunately, for most MEMS applications, only the low frequency behavior of the devices is of primary concern, i.e. only the viscous damping effect needs to be considered by designers. This low frequency characteristic of MEMS devices corresponds to the first scenario above, in which the natural frequency is very much lower than the cut-off frequency. In this case, the low frequency range of interest is below the natural frequency as the magnitude of the frequency response is decreasing very rapidly beyond the natural frequency. As the viscous damping effect is almost constant while the elastic damping effect is negligible, this case corresponds to the “incompressible gas” condition. As a result, the nature of the frequency response characteristics (underdamped, critically damped or overdamped system) is only determined by the damping ratio of the vibration system at low frequency. Under the “incompressible gas” condition, the coefficients of the viscous damping force for some common plate configurations (strip, circular, annular and rectangular) have been derived from Reynolds equation [45].

For the second case of $\omega_n \approx \omega_c$, both the viscous and elastic effects of the squeeze-film damping on the dynamic response are significant. The amplitude starts to fall considerably when the frequency approaches ω_c . The severity of this decline is dependent on the spring constant ratio of the squeeze-film damping effect to the system. The amount of viscous damping present in the system then determines whether there is a resonant peak and its magnitude at a frequency beyond ω_n .

For the third case of $\omega_n \gg \omega_c$, both the viscous and elastic effects of the squeeze-film damping on the dynamic response are usually small. The amplitude decreases slightly when the frequency approaches ω_c due to the increase of the elastic damping force. There will be a resonant peak at a frequency beyond ω_n and its peak can be high due to the small amount of viscous damping force at high frequency.

2.2.2 Border Effect of Plate Edges

The squeeze-film damping of parallel plates is derived with trivial boundary conditions, i.e. the gas at the edges (or borders) of the plates is at ambient pressure and there is no air flow escaping from the edges of the plates. This is a good approximation if the plate dimensions are much larger than the air film thickness. However, in practical cases, the air flow escaping from the plate edges may make a significant contribution to the total damping force. As such, the border effect on the squeeze-film damping of parallel plates has to be considered in the analysis. This can be done by the addition of elongations along the length and width of the plates. To include the border effect, an effective plate length and an effective plate width are introduced in the analysis such that the coefficient of the damping force for the enlarged plate with trivial boundary conditions has a similar value as that of the practical case with the included border effect [46–49].

2.2.3 Squeeze-film Damping of Perforated Plate

Plate structures, whether they are moving or stationary, in MEMS devices are occasionally perforated with holes or slots to reduce the squeeze-film damping effect to

achieve the desired performance specifications. The detrimental consequences of a large damping effect include low sensitivity, narrow bandwidth, high mechanical-thermal noise and slow switching speed. However, on the other hand, an increase in the damping effect can be utilized to widen the bandwidth performance of devices. Hence, an accurate assessment of the squeeze-film damping effect is very important during the initial design stage.

2.2.3.1 Infinite Thin Perforated Plate

For an infinite thin perforated plate, the squeeze-film damping can be easily analyzed with some approximations [2]. For simplicity, the plate is assumed to be perforated with circular holes, which are typically distributed evenly in a square or hexagon array. Then, the total damping force on the plate is the summation of the damping force of each individual cell. The cell is defined as the plate area that corresponds to each perforated hole. The two approximations used in the analysis are (1) the “infinite plate” approximation and (2) the “thin plate” approximation. Under the “infinite plate” approximation, the plate is much larger than a cell and all cells are identical, so that the air flow between the cells is negligible. Under the “thin plate” approximation, the plate is thin when compared with the size of the hole, so that the pressure difference causing the air flow through the hole is negligible. The squeeze-film damping analysis for an infinite thin perforated plate is applicable to thin plate structures that are fabricated by the surface micromachining technology.

2.2.3.2 Infinite Thick Perforated Plate

For an infinite thick perforated plate, for simplicity, the plate is considered infinite in lateral dimensions, so that the border effect of the plate edges is neglected and the analysis can be made for a cell containing a hole at the center. The analysis assumes that the air flows in and out of the plate only through the holes since the plate has infinite lateral dimensions. Thus, this means that the pressure distribution is identical for all the cells. Then, the total damping force on the plate can be obtained by multiplying the damping force of a cell with the number of cells (or holes) in the plate. The squeeze-film damping analysis for an infinite thick perforated plate is applicable to thick plate structures that are fabricated by the DRIE technology. For a finite thick perforated plate, an analysis involving the modified Reynolds equation for the whole plate is necessary to consider the border effect as a considerable amount of air may be squeezed out of the plates instead of through the holes only [45].

2.2.4 Effects of Compressibility, Inertia and Rarefied Gas

While the viscous force dominates the squeeze-film behavior at small frequencies, the compressibility of the gas and the inertia effect become more important in the determination of the spring force at higher frequencies. For the compressibility effect, the squeeze number denotes the degree of compression in the air gap. If the squeeze number is very much closer to zero (i.e. low frequency), the air film corresponds to the “incompressible gas” viscous flow regime. At the other end, at high frequencies, the air film is trapped in the air gap and behaves like a spring (“compressible gas” condition). If the air gap thickness is comparable to the mean free

path of the gas molecules or the pressure is lower than that of the ambient environment, the tangential velocity of the fluid at the boundary is no longer strictly zero. This phenomenon is known as the gas rarefaction effect (or the slip-flow effect). The slip velocity at the boundary gives rise to a decrease of the viscous forces in the squeezed air film.

The consideration of the compressibility, inertia and gas rarefaction effects [50] in the analysis of a MEMS ultrasonic transducer [51] results in the reduction of the error between the theoretical and experimental quality factor values of the diaphragm from 48% to 14%. In addition, a separate analysis of each individual effect reveals that the compressibility correction is the most influential effect of all while the inertia effect has to be considered only for the determination of both the spring and damping forces at sufficiently high frequencies. Finally, the gas rarefaction correction is only necessary for the case where the air gap thickness is significantly smaller than 2 μm .

2.2.5 Mechanical-thermal Noise

To examine the effect of the squeeze-film damping on the mechanical-thermal noise of an electrostatic sensor, a classical example of the condenser microphone will be drawn upon for illustration. The background noise level of a condenser microphone is one of the most important design specifications because it imposes a restriction on the microphone sensitivity. The background noise consists of both external sources, such as power supply lines, RF interference, and vibration, and internal generated ones such as mechanical-thermal noise [2, 52], electrical Johnson noise [52–54], shot noise [52–54], $1/f$ noise [52–55], and preamplifier noise [52, 53].

The movement of small movable mechanical components is adversely affected by molecular thermal agitation, which subsequently gives rise to the mechanical-thermal noise. If this noise is not adequately reined in, it can put a limiting factor on the performance of many small-sensor systems. The mechanical-thermal noise, together with other sources of background noise, establishes the lowest limit of acoustic pressure [53, 56] that can be picked up by the microphone.

A common modeling approach to evaluate the performance of a microphone is the lumped-element method [12–14, 16, 18, 20], in which through the use of simple analytical expressions, mass, compliance, and damping have their equivalent electrical counterparts in inductance, capacitance, and resistance, respectively. One method of reducing the mechanical-thermal noise is to reduce the mechanical resistance (or air resistance) through the meticulous design of the air gap, slot and holes in the backplate [57]. The background noise pressure level (*A*-weighted) of the mechanical-thermal noise [58] of a condenser microphone can be expressed by

$$N_T = \sqrt{\int_{f_1}^{f_2} 4k_B T R A^2(f) df} \quad (2.1)$$

where k_B is the Boltzmann constant (1.38×10^{-23} J/K), T is the absolute temperature (K), R is the total equivalent mechanical resistance of the air gap, slot, and holes, $A(f)$ is the function of the *A*-weighted filter, and f_1 and f_2 are 10 Hz and 20 kHz, respectively.

One of the more well-known mechanisms for the mechanical-thermal noise is the Brownian motion, which is the random, haphazard motion of small molecular particles in a constraint environment. Therefore, the agitation of a mechanical structure is a consequence of molecular collisions from the surrounding fluid molecules and it has a direct relationship with the fluid viscosity, which manifests itself in the

mechanical resistance expression. As with all molecular activities, the thermal effect has a significant influence on the molecular collisions. For the mechanical-thermal noise, this relationship is clearly illustrated in Eq. (2.1), in which there are a temperature component and a temperature-dependent viscosity term in the mechanical resistance component. The mechanical resistance increases with the temperature due to an increase in the viscosity. Hence, when the temperature increases, the mechanical-thermal noise increases for the sensor. In addition, for electrostatic-based sensors and actuators, an increase in temperature causes an increase in air viscosity, which subsequently results in a higher level of damping for the mechanical structures.

Hence, it is very obvious that the mechanical-thermal noise is dependent on the amount of viscous damping that is present in the mechanical structure. For the determination of the mechanical resistance, there are three main approaches.

2.2.5.1 Škvor's Approach

When holes are perforated in one of the plates, the viscous damping effect can be reduced significantly as the air flow has an alternative path to escape through the holes rather than being squeezed out from the edges of the plates. The equivalent mechanical resistance due to the perforated holes [59] is given by

$$R_{hole} = \frac{12\mu}{N\pi h^3} G(A) \quad [\text{Ns/m}^5] \quad (2.2)$$

where μ is the absolute viscosity of air ($17.9 \times 10^{-6} \text{ Ns/m}^2$), N is the number of holes in the plate, h is the unpolarized air gap thickness and $G(A)$ is given by

$$G(A) = \left[\frac{A}{2} - \frac{A^2}{8} - \frac{\ln A}{4} - \frac{3}{8} \right] \quad (2.3)$$

where A is the fraction of open area in the plate.

2.2.5.2 Gabrielson's Approach

In the approach of Gabrielson [2], the computation of the total equivalent mechanical resistance comprises of two portions: (1) Škvor's approach and (2) Starr's approach [60]. In the approach of Starr, for two parallel plates, the equivalent mechanical resistance due to squeeze-film damping [60] is given by

$$R_{airgap} = \frac{3\mu}{2\pi h^3} \quad [\text{Ns/m}^5] \quad (2.4)$$

For many applications, a good approximation of the total resultant mechanical resistance is obtained by the parallel combination of Eqs. (2.2) and (2.4) (hence, the definition of the Škvor/Starr approach). Notice how Eqs. (2.2) and (2.4) are heavily dependent on h and independent of frequency.

Most MEMS devices, such as microaccelerators, microphones, microspeakers and micromechanical switches, fall into the low frequency application domain ("incompressible gas" condition), in which the viscous damping effect (i.e. mechanical resistance) dominates. For high frequency application ("compressible gas" condition), such as ultrasonic transducers, the elastic damping effect (i.e. mechanical compliance) dominates and hence, Eqs. (2.2) and (2.4) are no longer applicable. To differentiate between the low frequency and high frequency regimes, the squeeze number [60] can be used and is given by

$$\sigma = \frac{12\mu\omega l^2}{P_a h^2} \quad (2.5)$$

where ω is the angular frequency, l is the characteristic length of the plate and P_a is the ambient pressure. As clearly indicated, a high frequency regime results in a high squeeze number. For a small value of σ (i.e. low frequency), the sandwiched air film behaves incompressibly. For a large value of σ , the air film behaves like a spring and exhibits little viscous damping characteristic. In general, when the squeeze number is much less than 0.2, the viscous damping effect dominates while the elastic damping effect can be neglected.

2.2.5.3 Zuckerwar's Approach

A more accurate, but more complicated, modeling approach has been described by Zuckerwar [61, 62]. In this comprehensive approach, an accurate air resistance term, which accounts for the mechanical damping losses of the air gap, slot, and holes in the backplate, is also provided. Well below the first resonant frequency of the diaphragm, the air resistance [61, 62] is given by

$$R_A(\omega) = \frac{8\pi T \text{Im}(D)}{\omega(\pi a^2)^2} \quad (2.6)$$

where ω is the angular frequency of the incident pressure, T is the diaphragm tension, a is the diaphragm radius and $D(\omega)$, which is the dimensionless air layer damping term, is given by

$$D(\omega) = \frac{j4\omega\rho_o J_o(Ka)}{\pi T J_2(Ka)} \sum_{m=0}^{\infty} \left[-j\omega \left(\frac{y_m}{y_o} \right) + \sum_{s=1}^q \left(\frac{f_s}{y_o} \right) s_s \Gamma_m J_o(\xi_m a_s) \right] \quad (2.7)$$

$$\times \frac{1}{T_m J_0(\xi_m a)} \left\{ \frac{J_2(Ka) \delta(m)}{4K^2 a^2} + \frac{J_1(Ka) [1 - \delta(m)]}{2Ka (K^2 a^2 - \xi_m^2 a^2)} \right\}$$

The detailed description of $D(\omega)$ will be provided in the latter section that follows. The validity of the air resistance term fails near the first resonant frequency of the membrane.

2.3 Critique of Existing Literature

The first paper on silicon electret condenser microphone in literature was described by Hohm *et al.* [10] in 1983. Six years later, Hohm *et al.* [11] also described the first silicon condenser microphone, which features a thin silicon nitride film as the pressure-sensitive diaphragm and a two-groove backplate structure to reduce the air streaming resistance. A few years later in 1992, Kühnel *et al.* [12, 13] reported a silicon condenser microphone, which consists of a silicon nitride diaphragm with plasma-etched grooves and anisotropically etched holes in the backplate, and a highly perforated thin backplate. Since then, a lot of research efforts have been directed to the selection of diaphragm materials, such as silicon nitride [14–16], doped polysilicon [17–19] and even polyamide [20], and the design and implementation of highly sensitive diaphragms, such as corrugated [21, 22] and spring-type [22] ones while relatively little emphasis is placed on the backplate design for the objective of damping optimization. The highly perforated backplate approach – numerous evenly-spaced holes, as a result of a smaller air gap thickness, are perforated in the backplate in a square array arrangement – is often the preferred choice for the backplate structures [13, 14, 16–22]. However, the simple and widely accepted practice of implementing a highly perforated backplate may not always be the best solution to achieve an optimum viscous damping value for MEMS devices.

Simple mechanical resistance expressions formulated by Škvor/Starr have been very widely used by many authors [12–14, 16, 18, 20]. The total mechanical resistance can be approximated by a parallel combination of the mechanical resistances of the squeeze-film damping (Starr [60]) and perforated holes in the backplate (Škvor [59]). Although the mechanical resistance expression is easy to apply, it does have its inherent limitations in that Škvor's mechanical resistance expression for the holes in the backplate only emphasizes the strong dependence on the total open area of holes, number of holes, and air gap thickness while the positional effect of holes on the mechanical resistance is not considered. This results in an overestimation of the total equivalent mechanical resistance and the resultant mechanical-thermal noise. In addition, the mechanical resistance expressions of Škvor and Starr are independent of frequency as there is an absence of a frequency component in their formulations.

However, on the other hand, a more accurate, but more complicated, modeling approach has been described by Zuckerwar [61, 62], which has the capability to describe such a hole location effect on the mechanical resistance. In this comprehensive approach, there are far more material properties and mechanical parameters than the Škvor/Starr approach, which also explains the complexity of Zuckerwar's approach. As a result, by looking at the formulations alone, it is not an easy task to establish a direct relationship between the mechanical parameters and the mechanical resistance unlike the Škvor/Starr approach, in which simple analytical expressions are used. Hence, lacking of a basic understanding of Zuckerwar's approach, with respect to the mechanical parameters, poses a hindrance to the effective

design of the backplate structure to control the viscous damping of the mechanical structures, so that desired performance characteristics can be achieved.

For example, Table 2.I compares the air resistance values of the B&K 0.5-inch (Type 4134) microphone from various sources. As tabulated, the Škvor approach and the Škvor/Starr approach provide resistance values of $2.66 \times 10^8 \text{ Ns/m}^5$ and $2.07 \times 10^8 \text{ Ns/m}^5$, respectively. Zuckerwar's more accurate approach gives a lower resistance value of $1.89 \times 10^8 \text{ Ns/m}^5$. From other data sources, Tarnow quotes a value of $1.54 \times 10^8 \text{ Ns/m}^5$ while Ngo uses a value of $1.25 \times 10^8 \text{ Ns/m}^5$.

Table 2.I. Comparison of the air resistance values (Škvor's, Škvor/Starr and Zuckerwar's approaches, and other data sources) based on the B&K 0.5-inch (Type 4134) microphone.

Source	$R \text{ (x } 10^8 \text{ Ns/m}^5\text{)}$
Škvor	2.66
Škvor/Starr [2]	2.07
Zuckerwar [61]	1.89 ^a
Tarnow [56]	1.54
Ngo [2]	1.25

^aTheoretical value at 250 Hz.

2.4 Proposed Solution

To address the effect of the hole location on the mechanical resistance, a modified form of the Škvor/Starr approach is proposed. A mode shape factor, which consists of the zero order Bessel and modified Bessel functions, is included in Škvor's mechanical resistance formulation to account for the hole location effect in the

backplate. The theoretical results obtained by the modified Škvor/Starr approach are then validated against two B&K condenser microphones.

In an endeavor to acquire a better perspective on the modeling approach of Zuckerwar, a 2^6 factorial design, which involves six backplate parameters and four responses, is carried out. Through this DOE technique, a valuable insight into the design optimization of the backplate structure is attained. Taking the developed backplate design optimization strategy a step further and using a PZT/Si acoustic transducer and a RF MEMS shunt switch as examples, it is demonstrated experimentally that the introduction or the deficiency of viscous damping in diaphragm-based mechanical structures can be exploited to enhance the performance characteristics of the devices. For the PZT/Si acoustic transducer, an increase in viscous damping, via the attachment of a silicon backplate, results in the widening of its bandwidth. For the RF MEMS shunt switch, a decrease in viscous damping, via the implementation of perforated holes in the base substrate, results in the lowering of its switching time.

2.5 Conclusion

With reference to a mass-spring-dashpot system, the key function of the damping effect in mechanical structures has been highlighted. A literature review on squeeze-film damping, which covers the aspects of frequency response characteristics, border effect of plate edges, viscous damping of perforated plate, effects of compressibility, inertia and rarefied gas, and mechanical-thermal noise, has also been briefly presented. A critique of the existing literature has been summarized while

solutions have been proposed to overcome these limitations. Depending on the nature of applications, the introduction or the deficiency of viscous damping in mechanical structures can be utilized to tune the mechanical performance of miniaturized devices in a positive manner.

References

- [1] T. Tschan, N. de Rooij and A. Bezing, "Damping of piezoresistive silicon accelerometers," *Sens. Actuators A*, vol. 32, pp. 375–379 (1992).
- [2] T. B. Gabrielson, "Mechanical-thermal noise in micromachined acoustic and vibration sensors," *IEEE Trans. Electron Devices*, vol. 40, pp. 903–909 (1993).
- [3] P. G. Steeneken *et al.*, "Dynamics and squeeze film gas damping of a capacitive RF MEMS switch," *J. Micromech. Microeng.*, vol. 15, pp. 176–184 (2005).
- [4] T. Tschan, N. de Rooij, A. Bezing, S. Ansermet and J. Berthoud, "Characterization and modelling of silicon piezoresistive accelerometers fabricated by a bipolar-compatible process," *Sens. Actuators A*, vol. 25–27, pp. 605–609 (1991).
- [5] Y. Matsumoto, M. Iwakiri, H. Tanaka, M. Ishida and T. Nakamura, "A capacitive accelerometer using SDB-SOI structure," *Sens. Actuators A*, vol. 53, pp. 267–272 (1996).
- [6] G. M. Sessler, "Acoustic sensors," *Sens. Actuators A*, vol. 25–27, pp. 323–330 (1991).
- [7] P. R. Scheeper, A. G. H. van der Donk, W. Olthuis and P. Bergveld, "A review of silicon microphones," *Sens. Actuators A*, vol. 44, pp. 1–11 (1994).
- [8] G. M. Sessler, "Silicon microphones," *J. Audio Eng. Soc.*, vol. 44, pp. 16–22 (1996).
- [9] C. Thielemann and G. M. Sessler, "Capacitive silicon sensors for ultrasound," *Acustica*, vol. 83, pp. 715–720 (1997).
- [10] D. Hohm and G. M. Sessler, "An integrated silicon-electret-condenser microphone," *Proc. 11th International Congress on Acoustics*, Paris, France, pp. 29–32, 1983.
- [11] D. Hohm and G. Hess, "A subminiature condenser microphone with silicon nitride membrane and silicon backplate," *J. Acoust. Soc. Am.*, vol. 85, pp. 476–480 (1989).
- [12] W. Kühnel and G. Hess, "A silicon condenser microphone with structured backplate and silicon nitride membrane," *Sens. Actuators A*, vol. 30, pp. 251–258 (1992).
- [13] W. Kühnel and G. Hess, "Micromachined subminiature condenser microphones in silicon," *Sens. Actuators A*, vol. 32, pp. 560–564 (1992).

-
- [14] P. R. Scheeper, W. Olthuis and P. Bergveld, "Improvement of the performance of microphones with a silicon nitride diaphragm and backplate," *Sens. Actuators A*, vol. 40, pp. 179–186 (1994).
- [15] P. R. Scheeper, B. Nordstrand, J. O. Gulløv, B. Liu, T. Clausen, L. Midjord and T. Storgaard-Larsen, "A new measurement microphone based on MEMS technology," *J. Microelectromech. S.*, vol. 12, pp. 880–891 (2003).
- [16] P. R. Scheeper, A. G. H. van der Donk, W. Olthuis and P. Bergveld, "Fabrication of silicon condenser microphones using single wafer technology," *J. Microelectromech. S.*, vol. 1, pp. 147–154 (1992).
- [17] J. Bergqvist and F. Rudolf, "A silicon condenser microphone using bond and etch-back technology," *Sens. Actuators A*, vol. 45, pp. 115–124 (1994).
- [18] P. C. Hsu, C. H. Mastrangelo and K. D. Wise, "A high sensitivity polysilicon diaphragm condenser microphone," *Proc. 11th IEEE Workshop on MEMS'98*, Heidelberg, Germany, pp. 580–585, 25–29 January 1998.
- [19] A. E. Kabir, R. Bashir, J. Bernstein, J. De Santis, R. Mathews, J. O. O'Boyle and C. Bracken, "High sensitivity acoustic transducers with thin p+ membranes and gold back-plate," *Sens. Actuators A*, vol. 78, pp. 138–142 (1999).
- [20] M. Pedersen, W. Olthuis and P. Bergveld, "A silicon condenser microphone with polyimide diaphragm and backplate," *Sens. Actuators A*, vol. 63, pp. 97–104 (1997).
- [21] W. J. Wang, R. M. Lin, Q. B. Zou and X. X. Li, "Modeling and characterization of a silicon condenser microphone," *J. Micromech. Microeng.*, vol. 14, pp. 403–409 (2004).
- [22] M. Földner, A. Dehé and R. Lerch, "Analytical analysis and finite element simulation of advanced membranes for silicon microphones," *IEEE Sens. J.*, vol. 5, pp. 857–863 (2005).
- [23] M. Royer, J. O. Holmen, M. A. Wurm and O. S. Aadland, "ZnO on Si integrated acoustic sensor," *Sens. Actuators A*, vol. 4, pp. 357–362 (1983).
- [24] E. S. Kim and R. S. Muller, "IC-processed piezoelectric microphone," *IEEE Electron Dev. Lett.*, vol. EDL-8, pp. 467–468 (1987).
- [25] R. P. Ried, E. S. Kim, D. M. Hong and R. S. Muller, "Piezoelectric microphone with on-chip CMOS circuits," *J. Microelectromech. S.*, vol. 2, pp. 111–120 (1993).

- [26] S. C. Ko, Y. C. Kim, S. S. Lee, S. H. Choi and S. R. Kim, "Micromachined piezoelectric membrane acoustic device," *Sens. Actuators A*, vol. 103, pp. 130–134 (2003).
- [27] C. H. Han and E. S. Kim, "Parylene-diaphragm piezoelectric acoustic transducers," *Proc. 13th IEEE Int. Conf. on MEMS*, Miyazaki, Japan, pp. 148–152, 23–27 January 2000.
- [28] S. H. Yi and E. S. Kim, "Piezoelectric microspeaker with compressive nitride diaphragm," *Proc. 15th IEEE Int. Conf. on MEMS*, Las Vegas, USA, pp. 260–263, 20–24 January 2002.
- [29] S. S. Lee, R. P. Ried and R. M. White, "Piezoelectric cantilever microphone and microspeaker," *J. Microelectromech. Syst.*, vol. 5, pp. 238–242 (1996).
- [30] R. Schellin and G. Hess, "A silicon subminiature microphone based on piezoresistive polysilicon strain gauges," *Sens. Actuators A*, vol. 32, pp. 555–559 (1992).
- [31] K. E. Petersen, "Micromechanical membrane switches on silicon," *IBM J. Res. Dev.*, vol. 23, pp. 376–385 (1979).
- [32] L. E. Larson, R. H. Hackett, M. A. Melendes and R. F. Lohr, "Micromachined microwave actuator (MIMAC) technology – A new tuning approach for microwave integrated circuits," *IEEE Microwave and Millimeter-Wave Monolithic Circuits Symposium*, Boston, MA, pp. 27–30, 10–11 June 1991.
- [33] D. Hyman *et al.*, "Surface-micromachined RF MEMS switches on GaAs substrates," *Int. J. RF Microwave CAE*, vol. 9, pp. 348–361 (1999).
- [34] P. M. Zavracky, N. E. McGruer, R. H. Morrison and D. Potter, "Microswitches and microrelays with a view toward microwave applications," *Int. J. RF Microwave CAE*, vol. 9, pp. 338–347 (1999).
- [35] R. E. Mihailovich *et al.*, "MEMS relay for reconfigurable RF circuits," *IEEE Microwave Wireless Components Lett.*, vol. 11, pp. 53–55 (2001).
- [36] S. Duffy, C. Bozler, S. Rabe, J. Knecht, L. Travis, P. Wyatt, C. Keast and M. Gouker, "MEMS microswitches for reconfigurable microwave circuitry," *IEEE Microwave Wireless Components Lett.*, vol. 11, pp. 106–108 (2001).
- [37] B. Schauwecker, K. M. Strohm, W. Simon, J. Mehner and J. F. Luy, "A new type of high bandwidth RF MEMS switch – Toggle switch," *J. Semicond. Technol. Sci.*, vol. 2, pp. 237–245 (2002).

- [38] J. Oberhammer and G. Stemme, "Design and fabrication aspects of an S-shaped film actuator based DC to RF MEMS switch," *J. Microelectromech. Syst.*, vol. 13, pp. 421–428 (2004).
- [39] C. L. Goldsmith, Z. Yao, S. Eshelman and D. Denniston, "Performance of low-loss RF MEMS capacitive switches," *IEEE Microwave Guided Wave Lett.*, vol. 8, pp. 269–271 (1998).
- [40] C. Chang and P. Chang, "Innovative micromachined microwave switch with very low insertion loss," *Sens. Actuators A*, vol. 79, pp. 71–75 (2000).
- [41] J. B. Muldavin and G. M. Rebeiz, "Inline capacitive and DC-contact MEMS shunt switches," *IEEE Microwave Wireless Components Lett.*, vol. 11, pp. 334–336 (2001).
- [42] J. Tsaur *et al.*, "Broadband MEMS shunt switches using PZT/HfO₂ multilayered high *k* dielectrics for high switching isolation," *Sens. Actuators A*, vol. 121, pp. 275–281 (2005).
- [43] Y. T. Song, H. Y. Lee and M. Esashi, "A corrugated bridge of low residual stress for RF-MEMS switch," *Sens. Actuators A*, vol. 135, pp. 818–826 (2007).
- [44] J. J. Blech, "On isothermal squeeze films," *ASME J. Lubric. Technol.*, vol. 105, pp. 615–620 (1983).
- [45] M. Bao and H. Yang, "Squeeze film air damping in MEMS," *Sens. Actuators A*, vol. 136, pp. 3–27 (2007).
- [46] T. Veijola, K. Ruokonen and I. Tittonen, "Compact model for the squeezed-film damping including the open border effects," *Proc. 4th Int. Conf. on MSM'01*, South Carolina, USA, pp. 76–79, 19–21 March 2001.
- [47] T. Veijola, "End effects of rare gas flow in short channels and in squeezed-film dampers," *Proc. 5th Int. Conf. on MSM'02*, San Juan, Puerto Rico, pp. 104–107, 22–25 April 2002.
- [48] T. Veijola, A. Pursula and P. Raback, "Extending the validity of squeezed-film damper models with elongations of surface dimensions," *J. Micromech. Microeng.*, vol. 15, pp. 1624–1636 (2005).
- [49] D. Homentcovschi and R. N. Miles, "Viscous microstructural dampers with aligned holes: Design procedure including the edge correction," *J. Acoust. Soc. Am.*, vol. 122, pp. 1556–1567 (2007).

- [50] D. Homentcovschi and R. N. Miles, "Analytical model for viscous damping and the spring force for perforated planar microstructures acting at both audible and ultrasonic frequencies," *J. Acoust. Soc. Am.*, vol. 124, pp. 175–181 (2008).
- [51] I. J. Oppenheim, A. Jain and D. W. Greve, "Electrical characterization of coupled and uncoupled MEMS ultrasonic transducers," *IEEE Trans. Ultrason., Ferroelect., Freq. Contr.*, vol. 50, pp. 297–304 (2003).
- [52] T. B. Gabrielson, "Fundamental noise limits in miniature acoustic and vibration sensors," *J. Vib. Acoust.*, vol. 117, pp. 405–410 (1995).
- [53] V. Tarnow, "Thermal noise in microphones and preamplifiers," *B&K Technical Review*, vol. 3, pp. 3–14 (1972).
- [54] A. J. Zuckerwar, T. R. Kuhn and R. M. Serbyn, "Background noise in piezoresistive, electret condenser and ceramic microphones," *J. Acoust. Soc. Am.*, vol. 113, pp. 3179–3187 (2003).
- [55] A. J. Zuckerwar and K. C. T. Ngo, "Measured $1/f$ noise in the membrane motion of condenser microphones," *J. Acoust. Soc. Am.*, vol. 95, pp. 1419–1425 (1994).
- [56] V. Tarnow, "The lower limit of detectable sound pressure," *J. Acoust. Soc. Am.*, vol. 82, pp. 379–381 (1987).
- [57] C. W. Tan, Z. H. Wang, J. M. Miao and X. F. Chen, "A study on the viscous damping effect for diaphragm-based acoustic MEMS applications," *J. Micromech. Microeng.*, vol. 17, pp. 2253–2263 (2007).
- [58] C. W. Tan and J. M. Miao, "Analytical modeling for bulk-micromachined condenser microphones," *J. Acoust. Soc. Am.*, vol. 120, pp. 750–761 (2006).
- [59] Z. Škvor, "On the acoustical resistance due to viscous losses in the air gap of electrostatic transducers," *Acustica*, vol. 19, pp. 295–299 (1967).
- [60] J. B. Starr, "Squeeze-film damping in solid-state accelerometers," *IEEE Workshop in Solid-State Sensor and Actuator 4th Technical Digest*, pp. 44–47 (1990).
- [61] A. J. Zuckerwar, "Theoretical response of condenser microphones," *J. Acoust. Soc. Am.*, vol. 64, pp. 1278–1285 (1978).
- [62] A. J. Zuckerwar, "Principles of operation of condenser microphones," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.

Chapter 3

Design Optimization of Microphone Backplate: A Design of Experiment Perspective

3.1 Introduction

For condenser microphones, an optimized viscous damping value has its origin in a well-designed backplate structure [1]. Thus, other than the function of an electrode, the backplate also has an important task in shaping the frequency response characteristics and determining the mechanical-thermal noise [2] of the microphone. A common modeling approach to evaluate the microphone performance is the lumped-element method [3–8]. Another approach is the finite element method (FEM) [9, 10]. An accurate, but more complicated, approach has been described by Zuckerwar [11, 12] and verified with very good accuracy [11–13]. This approach also provides an accurate expression for the air resistance term due to mechanical losses in the air gap, slot and holes.

A systematic investigation into the design optimization of the microphone backplate is carried out based on the comprehensive modeling approach of Zuckerwar via the DOE technique. The DOE technique is particularly helpful to learn more about a specific process or system with the ultimate aim of further optimization and has already demonstrated its capability in many engineering applications [14, 15]. So far, a detailed systematic optimization of the backplate parameters has not been reported. For previous reported work [3–10], the backplate parameters are determined on the basis of simple analytical expressions and the prior experience of the authors. However, with a

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systematic design methodology in place, any design optimization work can be simplified greatly.

A factorial design [16] is an effective method to study and screen out the main effects and interactions of several factors in an experiment setting. In a typical factorial design, every possible combination run of all involved factors can be investigated. One important special class of the factorial design is the 2^k factorial design, in which there are k factors at only two levels each. The 2^k factorial design is particularly useful in the initial stages of the research work, where there are many unknowns, to screen out the important factors for further optimization work. However, since there are only two levels for each factor, the assumption of almost linearity over the range of the factor levels may not be valid. In addition, any interaction terms can only contribute a little curvature to the response surface function. Thus, in some cases, the second order quadratic effects may have to be considered. Very often, limited resources only justify a single replicate of the design combination to be run. Then, a special case of the 2^k factorial design (unreplicated factorial design) occurs, in which only a single replicate is allowed in every run, with the assumption that the random error (or noise) in the system is reasonably small. Since there is only one replicate, there is no internal estimate of error.

In this chapter, since the primary objective is to study the design optimization of the backplate by identifying the main effects and any significant interactions via the 2^k factorial design, it is not strictly a must to consider the potential effect of non-linearity. And since the 2^k factorial design is carried out on analytical expressions, there are no random errors during the factorial run. Thus, the single replicate of the 2^k

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factorial design approach is the most appropriate for this design optimization work. If a main effect of an unreplicated 2^4 factorial design is not significant and all interactions involving that effect are negligible, then that main effect can be removed from the design so that it becomes a 2^3 factorial design with two replicates. Then, an analysis of variance (ANOVA) can be performed on the remaining main effects and interactions. Thus, this concept of projecting an unreplicated factorial design into a replicated one with fewer factors is very useful in the factorial design.

This chapter is divided into two main sections. Firstly, the condenser microphone theory is briefly reviewed. A MATLAB program is written based on the theory and verified against a B&K MEMS microphone [17]. Secondly, a 2^6 factorial design, which involves six backplate parameters and four responses, is carried out on the condenser microphone theory. These backplate parameter optimization data are subsequently applied during the design of the PZT/Si acoustic transducer (backplate) in Chapter 5 and the RF MEMS shunt switch (base substrate) in Chapter 6.

3.2 Condenser Microphone Theory

The theory of the condenser microphone will be briefly described, which follows the modeling approach of Zuckerwar [11, 12]. Detailed description of the theory can be found in Appendix A.

3.2.1. Microphone Sensitivity

The open-circuit sensitivity [11, 12] of the condenser microphone can be expressed by

$$M_{oc} = M_e \cdot M_m = \frac{E_o}{h_o} \left(1 - \frac{b^2}{2a^2} \right) \cdot \left(\frac{1}{TK^2} \frac{J_2(Ka)}{J_0(Ka) + D(\omega)} \right) \quad (3.1)$$

where M_e is the electrical sensitivity, M_m is the mechanical sensitivity, h_o is the average air gap distance between the diaphragm and the backplate due to the electrostatic deflection caused by the polarization voltage E_o , b is the backplate radius, a is the diaphragm radius, T is the diaphragm tension, ω is the angular frequency, and $J_0(Ka)$ and $J_2(Ka)$ are the zero and second order Bessel functions of Ka , respectively, while K , which is the wave number of sound in the diaphragm, and $D(\omega)$, which is the air layer damping term (a dimensionless quantity), can be expressed, respectively, by

$$K = \omega \left(\frac{\sigma_M}{T} \right)^{1/2} \quad (3.2)$$

where σ_M is the mass surface density of the diaphragm and

$$D(\omega) = \frac{j4\omega\rho_o J_o(Ka)}{\pi T J_2(Ka)} \sum_{m=0}^{\infty} \left[-j\omega \left(\frac{y_m}{y_o} \right) + \sum_{s=1}^q \left(\frac{f_s}{y_o} \right) s_s \Gamma_m J_o(\xi_m a_s) \right] \quad (3.3)$$

$$\times \frac{1}{T_m J_0(\xi_m a)} \left\{ \frac{J_2(Ka) \delta(m)}{4K^2 a^2} + \frac{J_1(Ka) [1 - \delta(m)]}{2Ka (K^2 a^2 - \xi_m^2 a^2)} \right\}$$

where ρ_o is the static air density (1.205 kg/m³) and $J_1(Ka)$ is the first order Bessel function of Ka . For further reading, the detailed mathematical derivation of $D(\omega)$ can be found in Refs. 11 and 12.

3.2.2 Mechanical-thermal Noise

The background noise of a microphone is one of the most important design specifications because it imposes a restriction on the microphone sensitivity. The

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mechanical-thermal noise, together with other sources of background noise [18–20], establishes the lowest possible limit of acoustic pressure [21, 22] that can be picked up by the microphone. Well below the first resonant frequency of the diaphragm, the air resistance [11, 12] can be expressed as

$$R_A(\omega) = \frac{8\pi T \text{Im}(D)}{\omega(\pi a^2)^2} \quad (3.4)$$

where $\text{Im}(D)$ is the imaginary part of $D(\omega)$. The air resistance R_A , which is due to the viscous damping losses in the air gap [23], slot and holes [24], accounts for the mechanical-thermal noise and it is frequency dependent. The background noise pressure level (A -weighted) of the mechanical-thermal noise [13] can be expressed by

$$N_T = \sqrt{\int_{f_1}^{f_2} 4k_B T R_A(f) A^2(f) df} \quad (3.5)$$

where k_B is the Boltzmann constant (1.38×10^{-23} J/K), T is the absolute temperature (K), $A(f)$ is the function of the A -weighted filter, and f_1 and f_2 are 10 Hz and 20 kHz, respectively. Therefore, the mechanical-thermal noise of the microphone is dependent on the amount of viscous damping that is present in the mechanical structure.

3.2.3 C_A/C_M Ratio

Well below the first resonant frequency of the diaphragm, the diaphragm compliance [11, 12] and air layer compliance [11, 12] can be expressed, respectively, as

$$C_M = \frac{(\pi a^2)^2}{8\pi T} \quad (3.6)$$

$$C_A(\omega) = \frac{(\pi a^2)^2}{8\pi T \operatorname{Re}(D)} \quad (3.7)$$

where $\operatorname{Re}(D)$ is the real part of $D(\omega)$. Therefore, the C_A/C_M ratio can be expressed as

$$\frac{C_A}{C_M} = \frac{1}{\operatorname{Re}(D)} \quad (3.8)$$

This ratio determines whether the diaphragm or the underlying air layer controls the microphone sensitivity. The diaphragm is the better choice as the air layer is much more temperature sensitive and less stable. For this to be realized, C_A should be larger than C_M .

3.3 Validation of Theoretical Results

Table 3.I lists the parameters of the B&K MEMS technology microphone while Fig. 3.1 illustrates its nomenclature. Table 3.II tabulates the theoretical results of the B&K MEMS microphone, in which the pull-in voltage, open-circuit sensitivity, bandwidth, and thermal noise demonstrate good agreements with those reported experimental ones found in Ref. [17]. Figure 3.2 illustrates the frequency response plot of the microphone, which again demonstrates good agreement between the theoretical and measured responses. For the noise analysis of the B&K MEMS microphone, only the preamplifier noise and the mechanical-thermal noise will be considered. The preamplifier noise dominates the total microphone noise below 500 Hz while the mechanical-thermal noise dominates from 500 Hz to 30 kHz. From Ref. [17], the preamplifier noise data points at 200 V polarization voltage are plotted until 400 Hz, curve fitted and then extrapolated from 500 Hz to 40 kHz. For curve fitting, a power curve is used ($R^2 = 0.98$). Next, the extrapolated preamplifier noise is quadratically

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subtracted from the experimental total noise at 200 V polarization voltage to attain the experimental mechanical-thermal noise spectrum. Figure 3.3 illustrates the mechanical-thermal noise spectrum of the microphone, in which the theoretical mechanical-thermal noise is in good agreement with the experimental one. Finally, the extrapolated preamplifier noise is quadratically added to the theoretical mechanical-thermal noise to yield the theoretical total noise spectrum. Figure 3.4 illustrates the total noise spectrum of the microphone, in which a good agreement is also observed between the theoretical and experimental results. This validates the accuracy of the written MATLAB codes.

Table 3.I. Parameters [17] of the B&K MEMS technology condenser microphone.

Parameter	Symbol	Value
Diaphragm radius	a	1.95 mm
Diaphragm thickness	d	0.5 μm
Diaphragm mass surface density	σ_M	0.0015 kg/m ²
Diaphragm tension	T	170 N/m
Backplate radius	b	1.4 mm
Unpolarized air gap	h	20 μm
Backchamber volume	V	7.6 x 10 ⁻⁸ m ³
Acoustic slot ($k = 1$)		
Slot location	a_1	1.675 mm
Slot width	r_1	0.55 mm
Slot depth	l_1	150 μm
Acoustic holes ($k = 2$)		
Number of holes per radius ring	b_2	4
Radius ring location ^a	a_2	0.55 mm
Hole radius ^a	r_2	40 μm
Hole depth	l_2	150 μm
Polarization voltage	E_o	200 V
Microphone capacitance	C_{mic}	3.5 pF
Preamplifier input capacitance	C_i	0.4 pF
Stray capacitance	C_s	5.1 pF

^aApproximated from the optical microscope photograph in Ref. [17].

Fig. 3.1. B&K MEMS condenser microphone: (a) Cross-sectional view and (b) arrangement of acoustic slot and holes in the backplate.

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Table 3.II. Theoretical and experimental results of the B&K MEMS technology microphone.

Parameter	Symbol	Theoretical	Experimental (Ref. [17])
Pull-in voltage	V_{in}	254 V	255 to 263 V
Open-circuit sensitivity	M_{oc}	23 mV/Pa -33 dB ref. 1 V/Pa	22 mV/Pa -33 dB ref. 1 V/Pa
Overall sensitivity ^a	M	9.4 mV/Pa	-
Bandwidth	BW	15 kHz	15 kHz
Membrane mass	M_M	167 kg/m ⁴	-
Membrane compliance	C_M	3.3×10^{-14} m ⁵ /N	-
Air resistance ^b	R_A	3.2×10^8 Ns/m ⁵	-
Air compliance ^b	C_A	5.4×10^{-13} m ⁵ /N	-
Thermal noise	N	23 dB(A)	23 dB(A)
Signal-to-noise ratio ^c	SNR	71 dB	-

^aAssume unity preamplifier gain.^bTheoretical value at 250 Hz.^cTheoretical value with reference to 1 Pa at 1 kHz.

Fig. 3.2. Frequency response plot of the B & K MEMS technology microphone. Measured response is taken from Ref. [17].

Fig. 3.3. Theoretical and experimental mechanical-thermal noise spectrum of the B&K MEMS technology microphone. Experimental data are taken from Ref. [17] after the quadratic subtraction of the preamplifier noise from the total noise.

Fig. 3.4. Theoretical and experimental total noise spectrum of the B&K MEMS technology microphone. Experimental data are taken from Ref. [17].

3.4 Design of Experiment

Table 3.III tabulates the factor assignment of the six selected backplate parameters – air gap (A), absence or presence of a slot (B), hole location (C), hole number (D), hole radius (E) and backplate thickness (F) – and their respective assigned values. The hole location is defined by one radius ring from the backplate center. Other microphone parameters [13, 17] used for the factorial design are listed in Table 3.IV. From Table 3.IV, a diaphragm value of 170 N/m is selected so that an analysis can be performed on a workable microphone design [17]. For the 2^6 factorial design, a total of 64 single-replicate simulation runs have been performed as tabulated partially in Table 3.V. The complete tabulation of all 64 single-replicate simulation runs can be found in Appendix B. The four desired responses are the open-circuit sensitivity (250 Hz), A -weighted mechanical-thermal noise, bandwidth (± 3 dB) and C_A/C_M ratio (250 Hz).

3.4.1 Open-circuit Sensitivity

Figure 3.5 illustrates the normal probability plot of the effect estimates for the open-circuit sensitivity for the single-replicate 2^6 factorial design. Negligible effects, which are normally distributed, tend to lie along a straight line while significant effects are far away from it. Thus, the normal probability plot is an excellent method to identify and isolate the main effects and any important interactions of the factorial design. From Fig. 3.5, the significant effects are the main effects of A (-2.450) and B (-3.350) without any significant interactions. Figure 3.6 illustrates the main effects of A and B and both are negative. Therefore, a high sensitivity can be attained when A and B are at the low level.

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Table 3.III. Assignment of factors and backplate parameters in the 2^6 factorial design. The parameters under the “-” column are taken from a workable microphone design [13, 17] while those under the “+” column are arbitrary values.

Parameter	Assigned factor	-	+
Air gap	<i>A</i>	20 μm	25 μm
Slot	<i>B</i>	Presence	Absence
Hole location	<i>C</i>	0.55 mm	1.2 mm
Hole number	<i>D</i>	4	8
Hole radius	<i>E</i>	40 μm	80 μm
Backplate thickness	<i>F</i>	150 μm	300 μm

Table 3.IV. Other microphone parameters used for the factorial design.

Parameter	Symbol	Value
Diaphragm radius	<i>a</i>	1.95 mm
Diaphragm thickness	<i>d</i>	0.5 μm
Diaphragm mass surface density	σ_M	0.0015 kg/m ²
Diaphragm tension	<i>T</i>	170 N/m
Backplate radius	<i>b</i>	1.4 mm
Backchamber volume	<i>V</i>	7.6 x 10 ⁻⁸ m ³
Polarization voltage	<i>E_o</i>	200 V
Preamplifier input capacitance	<i>C_i</i>	0.4 pF
Stray capacitance	<i>C_s</i>	5.1 pF

Since the main effects of *C*, *D*, *E* and *F* are not significant and all interactions involving them are negligible, they can be discarded from the 2^6 factorial design so that it becomes a 2^2 factorial design in *A* and *B* with 16 replicates. Table 3.VI tabulates the open-circuit sensitivity data (rearranged from Table 3.V) of the 2^2 factorial design in *A* and *B* with 16 replicates. As Table 3.VI only tabulates the average sensitivity data of the 16 replicates, it does not reveal that all 16 replicates of each run have the same value. However, these are revealed upon closer inspection of Table 3.V under the

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Table 3.V. Partial list of data of the single-replicate ($N=1$) 2^6 factorial design.

Run Number	Factor A	Factor B	Factor C	Factor D	Factor E	Factor F	Run Label	Open-circuit sensitivity (dB ref. 1 V/Pa)	Bandwidth (kHz)	Mechanical-thermal noise [dB(A)]	C_A/C_M ratio
1	-	-	-	-	-	-	(1)	-33.2	15.0	23.0	16.1
2	+	-	-	-	-	-	a	-35.6	20.0	19.8	16.1
3	-	+	-	-	-	-	b	-36.5	2.5	30.6	16.1
4	+	+	-	-	-	-	ab	-39.0	5.0	27.4	16.3
5	-	-	+	-	-	-	c	-33.2	7.5	25.7	16.2
6	+	-	+	-	-	-	ac	-35.6	12.0	22.6	16.2
:	:	:	:	:	:	:	:	:	:	:	:
:	:	:	:	:	:	:	:	:	:	:	:
59	-	+	-	+	+	+	bdef	-36.5	2.5	30.4	15.9
60	+	+	-	+	+	+	abdef	-39.0	6.0	27.1	16.1
61	-	-	+	+	+	+	cdef	-33.2	10.0	25.4	16.1
62	+	-	+	+	+	+	acdef	-35.6	18.0	22.0	16.1
63	-	+	+	+	+	+	bcdef	-36.5	10.0	25.6	16.1
64	+	+	+	+	+	+	abcdef	-39.0	16.0	22.4	16.2

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sensitivity data column, in which the four values of -33.2, -35.6, -36.5 and 39.0 keep repeating themselves in a cycle. The ANOVA for the data after the projection of the unreplicated factorial design into a replicated one with two factors is summarized in Table 3.VII. Table 3.VII cannot be completed – F_0 values cannot be computed – because the sum of squares of the error is zero. This can be attributed to all 16 replicates of each of the four runs in Table 3.VI sharing similar value as noted above. Nevertheless, by looking at the mean square values in Table 3.VII, the significant effects are the main effects of A and B without any significant interaction, a similar conclusion that is also revealed by the normal probability plot of Fig. 3.5.

Fig. 3.5. Normal probability plot of the effect estimates for the open-circuit sensitivity for the single-replicate 2^6 factorial design.

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Fig. 3.6. Main effect plots for the open-circuit sensitivity.

Table 3.VI. Open-circuit sensitivity data of 2^2 factorial design in A and B with 16 replicates.

Run Number	Factor A	Factor B	Average open-circuit sensitivity (dB ref. 1 V/Pa) ($N=16$)
1	-	-	-33.2
2	+	-	-35.6
3	-	+	-36.5
4	+	+	-39.0

Table 3.VII. ANOVA for the open-circuit sensitivity in A and B .

Source of Variation	Sum of Squares	Degrees of Freedom	Mean Square	F_0
A (Air gap)	96.04	1	96.04	-
B (Slot)	179.56	1	179.56	-
AB	0.04	1	0.04	-
Error	0	60	0	
Total	275.64	63		

3.4.2 A -weighted Mechanical-thermal Noise

Figure 3.7 illustrates the normal probability plot of the effect estimates for the A -weighted mechanical-thermal noise for the single-replicate 2^6 factorial design. From Fig. 3.7, the significant effects are the main effects of A (-3.188) and B (4.006) and the BC interaction (-3.613). Figure 3.8 illustrates the main effects of A , B and C and the interaction effect of BC . The main effect of C is relatively insignificant (-0.937) when

compared to that of A and B . From Fig. 3.8(b), there is a significant interaction between B and C . When there are significant interactions, examination of the main effects alone is insufficient. As a result, both the main effects and the interactions have to be considered together. Therefore, a low mechanical-thermal noise can be attained when A is at the high level and B and C are at the low level.

Since the main effects of D , E and F are not significant and all interactions involving them are negligible, they are discarded from the 2^6 factorial design so that it becomes a 2^3 factorial design in A , B and C with 8 replicates. Table 3.VIII tabulates the A -weight mechanical-thermal noise data of the 2^3 factorial design in A , B and C with 8 replicates. The ANOVA for the data after the projection of the unreplicated factorial design into a replicated one with three factors is summarized in Table 3.IX. Since $F_{0.05, 1, 56} \approx 4.02$, the significant effects are the main effects of A and B and the BC interaction, which are also verified by the normal probability plot of Fig. 3.7.

Fig. 3.7. Normal probability plot of the effect estimates for the A -weighted mechanical-thermal noise for the single-replicate 2^6 factorial design.

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Fig. 3.8. Main effect and interaction plots for the *A*-weighted mechanical-thermal noise: (a) *A*, *B* and *C* and (b) *B*-*C* interaction.

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Table 3.VIII. *A*-weighted mechanical-thermal noise data of 2^3 factorial design in *A*, *B* and *C* with 8 replicates.

Run Number	Factor <i>A</i>	Factor <i>B</i>	Factor <i>C</i>	Average mechanical-thermal noise [dB(A)] (<i>N</i> =8)
1	-	-	-	22.9
2	+	-	-	19.6
3	-	+	-	30.5
4	+	+	-	27.3
5	-	-	+	25.6
6	+	-	+	22.3
7	-	+	+	25.9
8	+	+	+	22.8

Table 3.IX. ANOVA for the *A*-weighted mechanical-thermal noise in *A*, *B* and *C*.

Source of Variation	Sum of Squares	Degrees of Freedom	Mean Square	<i>F</i> ₀
<i>A</i> (Air gap)	162.56	1	162.56	2032.00
<i>B</i> (Slot)	256.80	1	256.80	3210.00
<i>C</i> (Hole location)	14.06	1	14.06	175.75
<i>AB</i>	0.09	1	0.09	1.13
<i>AC</i>	0.05	1	0.05	0.63
<i>BC</i>	208.80	1	208.80	2610.00
<i>ABC</i>	0.02	1	0.02	0.25
Error	4.71	56	0.08	
Total	647.09	63		

3.4.3 Bandwidth

Figure 3.9 illustrates the normal probability plot of the effect estimates for the bandwidth for the single-replicate 2^6 factorial design. From Fig. 3.9, the most significant effects are the main effects of *A* (5.117), *B* (-9.227) and *E* (3.820) and the *BC* interaction (7.648) while the least significant effects are the main effects of *C* (-

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1.539), D (1.680) and F (-1.195) and the interaction effects of AB (-1.820), AE (1.352), BE (-1.117) and BCE (1.695). Figure 3.10 illustrates the main effects of A , B , C and E and the interaction effect of BC . The main effect of C is the least significant of all four factors while the main effects of A and B are larger than that of E . Therefore, a large bandwidth can be attained when A and E are at the high level and B and C are at the low level.

Fig. 3.9. Normal probability plot of the effect estimates for the bandwidth for the single-replicate 2^6 factorial design.

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(a)

(b)

Fig. 3.10. Main effect and interaction plots for the bandwidth: (a) *A*, *B*, *C* and *E* and (b) *B*-*C* interaction.

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Since the main effects of D and F are not significant and all interactions involving them are negligible, they are discarded from the 2^6 factorial design so that it becomes a 2^4 factorial design in A , B , C and E with 4 replicates. Table 3.X tabulates the bandwidth data of the 2^4 factorial design in A , B , C and E with 4 replicates. The ANOVA for the data after the projection of the unreplicated factorial design into a replicated one with four factors is summarized in Table 3.XI. From Table 3.XI, since $F_{0.05, 1, 48} \approx 4.05$, the most significant effects are the main effects of A , B and E and the BC interaction while the least significant effects are the main effects of C , D and F and the AB , AE , BE and BCE interactions.

Table 3.X. Bandwidth data of 2^4 factorial design in A , B , C and E with 4 replicates.

Run Number	Factor A	Factor B	Factor C	Factor E	Average bandwidth (kHz) ($N=4$)
1	-	-	-	-	14.9
2	+	-	-	-	20.8
3	-	+	-	-	2.5
4	+	+	-	-	5.0
5	-	-	+	-	7.5
6	+	-	+	-	12.3
7	-	+	+	-	6.8
8	+	+	+	-	8.7
9	-	-	-	+	19.1
10	+	-	-	+	28.9
11	-	+	-	+	2.6
12	+	+	-	+	6.0
13	-	-	+	+	9.9
14	+	-	+	+	17.3
15	-	+	+	+	9.9
16	+	+	+	+	15.3

Table 3.XI. ANOVA for the bandwidth in A , B , C and E .

Source of Variation	Sum of Squares	Degrees of Freedom	Mean Square	F_0
A (Air gap)	418.97	1	418.97	163.02
B (Slot)	1362.07	1	1362.07	529.99
C (Hole location)	37.90	1	37.90	14.75
E (Hole radius)	233.52	1	233.52	90.86
AB	53.02	1	53.02	20.63
AC	1.06	1	1.06	0.41
AE	29.23	1	29.23	11.37
BC	935.98	1	935.98	364.19
BE	19.97	1	19.97	7.77
CE	3.17	1	3.17	1.23
ABC	6.09	1	6.09	2.37
ABE	1.20	1	1.20	0.47
ACE	0.43	1	0.43	0.17
BCE	45.99	1	45.99	17.89
$ABCE$	3.63	1	3.63	1.41
Error	123.17	48	2.57	
Total	3275.40	63		

3.4.4. C_A/C_M Ratio

From Table 3.V, the C_A/C_M ratio is around 16 for all simulation runs, which indicate that the diaphragm controls the sensitivity. Figure 3.11 illustrates the normal probability plot of the effect estimates for the C_A/C_M ratio for the single-replicate 2^6 factorial design. From Fig. 3.11, there are no significant main effects and interactions. As such, the ANOVA on the C_A/C_M ratio data is not necessary.

Fig. 3.11. Normal probability plot of the effect estimates for the C_A/C_M ratio for the single-replicate 2^6 factorial design.

3.5 Results and Discussions

3.5.1 Optimized factors for Best Microphone Performance

Table 3.XII illustrates a summary of the main effects and interactions for the open-circuit sensitivity, A -weighted mechanical-thermal noise and bandwidth. For best microphone performance, the backplate parameters should be set to A^- and B^- for sensitivity, A^+ , B^- and C^- for mechanical-thermal noise and A^+ , B^- , C^- and E^+ for bandwidth. Upon closer examination, a design conflict involving the air gap (A) is clearly observed. A smaller air gap (A^-) is required for higher sensitivity but it is at the expense of narrower bandwidth and higher mechanical-thermal noise. On the contrary, a larger air gap (A^+) is desired for lower mechanical-thermal noise and wider

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bandwidth but it is at the expense of lower sensitivity. In addition, if the air gap is too large, a resonant peak can be observed at the resonant frequency coupled with further loss in sensitivity. Thus, there appears to be an optimized air gap value to ensure a good performance compromise between the sensitivity, mechanical-thermal noise and bandwidth [13]. From Table 3.XII, it is obvious that the presence of a slot (*B*-) around the circumference of the backplate enhances both the sensitivity and bandwidth and lowers the mechanical-thermal noise [13].

From Table 3.XII, it is also noted that the most significant interaction among all parameters is the *BC* (slot/hole location) interaction, which influences both the mechanical-thermal noise and bandwidth. This can be attributed to the fact that both holes and slots make the backplate acoustically a surface of extended, not local, reaction since the airflow through one hole or slot is coupled to the flows through all the other openings in the backplate to the backchamber.

Table 3.XII. Summary of the main and interaction effects for the open-circuit sensitivity, *A*-weighted mechanical-thermal noise and bandwidth.

Performance parameter	Most significant main effects	Least significant main effects	Most significant interactions	Least significant interactions	Factor level for best performance
Open-circuit sensitivity	<i>A</i> (-2.450) <i>B</i> (-3.350)	-	-	-	<i>A</i> - <i>B</i> -
Mechanical-thermal noise	<i>A</i> (-3.188) <i>B</i> (4.006)	<i>C</i> (-0.937)	<i>BC</i> (-3.613)	-	<i>A</i> + <i>B</i> - <i>C</i> -
Bandwidth	<i>A</i> (5.117) <i>B</i> (-9.227) <i>E</i> (3.820)	<i>C</i> (-1.539) <i>D</i> (1.680) <i>F</i> (-1.195)	<i>BC</i> (7.648)	<i>AB</i> (-1.820) <i>AE</i> (1.352) <i>BE</i> (-1.117) <i>BCE</i> (1.695)	<i>A</i> + <i>B</i> - <i>C</i> - <i>E</i> +

3.5.2 Backplate Design Optimization

It should be apparent by now that Table 3.XII presents an organized and systematic methodology of optimizing the backplate design. As shown, the main effects of A (air gap) and B (slot) appear to exert a significant influence on all three performance parameters while the BC (slot/hole location) interaction affects the mechanical-thermal noise and bandwidth only. The main effect of E (hole radius) also has an influence over the bandwidth but it is not as significant as A and B . After the determination of the microphone size (i.e. diaphragm size), the air gap thickness (A) and the slot width (B) are determined next to achieve the desired open-circuit sensitivity. For the slot width, there is an optimum size criterion [12, 25], which states that the ratio of the backplate radius to the diaphragm radius is 0.82. For the air gap thickness, although a small air gap is desired for high sensitivity (bearing in mind that a small air gap is detrimental to both the mechanical-thermal noise and the bandwidth), it must not be so small that both the mechanical-thermal noise and the bandwidth enhancements cannot be made by exploiting the other remaining main effects. Thus, the selected air gap thickness should be a good design compromise between the sensitivity, mechanical-thermal noise and bandwidth.

In order to make further enhancement to the mechanical-thermal noise after the determination of the air gap thickness and the slot width, the main effect of C (hole location) should be optimized due to the significant BC interaction. The remaining parameters of D , E and F have negligible influence on the mechanical-thermal noise.

Likewise, the bandwidth enhancement can also be simultaneously made with the mechanical-thermal noise enhancement by manipulating the main effect of C . After

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the main effect of C , further enhancement to the bandwidth can be achieved by optimizing the remaining main effects of D , E and F . The order of parameter optimization is suggested by their effect estimates as tabulated in Table 3.XII. Since the main effect of E (hole radius) has the highest effect estimate (3.820) of all, it should be given the first priority for optimization. By this reasoning, the next in line for optimization is the main effect of D (hole number) (1.680) and finally, F (backplate thickness) (-1.195).

To summarize, firstly, the main effects of A and B are used to maximize the open-circuit sensitivity with due consideration given to the detrimental effect of a narrow air gap on the mechanical-thermal noise and the bandwidth. Secondly, for the enhancement of both the mechanical-thermal noise and the bandwidth, the main effect of C is manipulated next by virtue of the BC interaction. Finally, the main effects of E , D and F , in that order, are used to fine-tune the bandwidth to a desired value.

3.6 Conclusion

The condenser microphone theory has been briefly reviewed and a MATLAB program has been coded to implement the theory. With reference to a B&K MEMS microphone, the theoretical results obtained are in very good agreements with those reported experimental ones. A 2^6 factorial design with a single replicate has been carried out on a comprehensive condenser microphone model on the basis of six backplate design parameters and four output responses. By eliminating the insignificant parameters via normal probability plots of the effect estimates, a replicated factorial design is obtained so that an ANOVA on the factorial design can be performed. The

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main effects of air gap and slot are found to have significant influences over the sensitivity, mechanical-thermal noise and bandwidth while the slot/hole location interaction has significant effects on the latter two responses. The remaining main effects of hole number, hole size and backplate thickness have only minor influences over the bandwidth. For the C_A/C_M ratio, there are no significant main effects and interactions. The DOE optimization approach also demonstrates the robustness of the condenser microphone modeling approach of Zuckerwar. Finally, an organized and systematic approach of designing the backplate is summarized.

References

- [1] C. W. Tan, Z. H. Wang, J. M. Miao and X. F. Chen, "A study on the viscous damping effect for diaphragm-based acoustic MEMS applications," *J. Micromech. Microeng.*, vol. 17, pp. 2253–2263 (2007).
- [2] T. B. Gabrielson, "Mechanical-thermal noise in micromachined acoustic and vibration sensors," *IEEE Trans. Electron Devices*, vol. 40, pp. 903–909 (1993).
- [3] P. R. Scheeper, A. G. H. van der Donk, W. Olthuis and P. Bergveld, "Fabrication of silicon condenser microphones using single wafer technology," *J. Microelectromech. Syst.*, vol. 1, pp. 147–154 (1992).
- [4] W. Kühnel and G. Hess, "A silicon condenser microphone with structured backplate and silicon nitride membrane," *Sens. Actuators A*, vol. 30, pp. 251–258 (1992).
- [5] P. R. Scheeper, W. Olthuis and P. Bergveld, "Improvement of the performance of microphones with a silicon nitride diaphragm and backplate," *Sens. Actuators A*, vol. 40, pp. 179–186 (1994).
- [6] M. Pedersen, W. Olthuis and P. Bergveld, "A silicon condenser microphone with polyimide diaphragm and backplate," *Sens. Actuators A*, vol. 63, pp. 97–104 (1997).
- [7] M. Pedersen, W. Olthuis and P. Bergveld, "High-performance condenser microphone with fully integrated CMOS amplifier and DC-DC voltage converter," *J. Microelectromech. Syst.*, vol. 7, pp. 387–394 (1998).
- [8] R. Nadal-Guardia, A. M. Brosa and A. Dehé, "AC transfer function of electrostatic capacitive sensors based on the 1-D equivalent model: Application to silicon microphones," *J. Microelectromech. Syst.*, vol. 12, pp. 972–978 (2003).
- [9] J. Bergqvist and F. Rudolf, "A silicon condenser microphone using bond and etch-back technology," *Sens. Actuators A*, vol. 45, pp. 115–124 (1994).
- [10] X. Li, R. Lin, H. S. Kek, Jianmin Miao and Q. Zou, "Sensitivity-improved silicon condenser microphone with a novel single deeply corrugated diaphragm," *Sens. Actuators A*, vol. 92, pp. 257–262 (2001).
- [11] A. J. Zuckerwar, "Theoretical response of condenser microphones," *J. Acoust. Soc. Am.*, vol. 64, pp. 1278–1285 (1978).

Chapter 3 Design Optimization of Microphone Backplate: A DOE Perspective

- [12] A. J. Zuckerwar, "Principles of operation of condenser microphones," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.
- [13] C. W. Tan and J. M. Miao, "Analytical modeling for bulk-micromachined condenser microphone", *J. Acoust. Soc. Am.*, vol. 120, pp. 750–761 (2006).
- [14] L. P. Yeo, Y. H. Yan, Y. C. Lam and M. B. Chan-Park, "Design of experiment for optimization of plasma-polymerized octafluorocyclobutane coating on very high aspect ratio silicon molds", *Langmuir*, vol. 22, pp. 10196–10203 (2006).
- [15] Z. W. Zhong, Z. F. Wang and Y. H. Tan, "Chemical mechanical polishing of polymeric materials for MEMS applications", *Microelectron. J.*, vol. 37, pp. 295–301 (2006).
- [16] D. C. Montgomery, *Design and Analysis of Experiments* (John Wiley & Sons, New York, 1997).
- [17] P. R. Scheeper, B. Nordstrand, J. O. Gulløv, B. Liu, T. Clausen, L. Midjord and T. Storgaard-Larsen, "A new measurement microphone based on MEMS technology," *J. Microelectromech. Syst.*, vol. 12, pp. 880–891 (2003).
- [18] T. B. Gabrielson, "Fundamental noise limits in miniature acoustic and vibration sensors," *J. Vib. Acoust.*, vol. 117, pp. 405–410 (1995).
- [19] A. J. Zuckerwar, T. R. Kuhn and R. M. Serbyn, "Background noise in piezoresistive, electret condenser and ceramic microphones," *J. Acoust. Soc. Am.*, vol. 113, pp. 3179–3187 (2003).
- [20] A. J. Zuckerwar and K. C. T. Ngo, "Measured $1/f$ noise in the membrane motion of condenser microphones," *J. Acoust. Soc. Am.*, vol. 95, pp. 1419–1425 (1994).
- [21] V. Tarnow, "Thermal noise in microphones and preamplifiers," *B&K Technical Review*, vol. 3, pp. 3–14 (1972).
- [22] V. Tarnow, "The lower limit of detectable sound pressure," *J. Acoust. Soc. Am.*, vol. 82, pp. 379–381 (1987).
- [23] J. B. Starr, "Squeeze-film damping in solid-state accelerometers," *IEEE Workshop in Solid-State Sensor and Actuator 4th Technical Digest*, pp. 44–47 (1990).
- [24] Z. Škvor, "On the acoustical resistance due to viscous losses in the air gap of electrostatic transducers," *Acustica*, vol. 19, pp. 295–299 (1967).

Chapter 3 Design Optimization of Microphone Backplate: A DOE Perspective

- [25] M. S. Hawley, F. F. Romanow and J. E. Warren, “The Western Electric 640AA capacitance microphone: Its history and theory of operation,” in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 2, pp. 8–34.

Chapter 4

Modified Škvor/Starr Approach in the Mechanical Resistance Analysis of Squeeze-film Damping

4.1 Introduction

The minute movement of miniaturized mechanical components is adversely affected by molecular thermal agitation, which subsequently gives rise to the mechanical-thermal noise [1]. If this mechanical-thermal noise is not adequately controlled, it can put a limiting factor on the noise performance of many small-sensor systems. The physical origin of the mechanical-thermal noise lies in the dissipation mechanisms such as the damping in springs, air damping between two parallel surfaces, and viscous drag in fluids. For the condenser microphone, the dissipation mechanism is represented by the viscous damping losses in the air gap, and slot and holes in the backplate. The mechanical-thermal noise, together with other sources of background noise [2–4], establishes the lowest limit of acoustic pressure [5, 6] that can be picked up by a microphone. In addition, the dissipation mechanism also plays an important role in defining the frequency response characteristics of the microphone. Therefore, an optimized viscous damping value has its origin in a well-designed backplate structure [7].

A common modeling approach to evaluate the performance of a microphone is the lumped-element method [8–13]. In this approach, through the use of simple analytical expressions, mass, compliance, and damping have their equivalent electrical counterparts in inductance, capacitance, and resistance, respectively. The lumped-

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element method, as exemplified by the analysis of Gabrielson [1], has been very popular with many authors [8–13]. In his approach, the total resistance is approximated by a parallel combination of the equivalent mechanical resistances of squeeze-film damping (Starr [14]) and holes in the backplate (Škvor [15]). Although the mechanical resistance expression is easy to apply, it does have its inherent limitations. The expression for the mechanical resistance of holes in the backplate only emphasizes the strong dependence on the total open area of holes, number of holes, and air gap thickness while the positional effect of holes on the mechanical resistance is not considered and properly addressed. An accurate, but more complicated, modeling approach has been described by Zuckerwar [16, 17] and verified in Refs. 16–18 with very good accuracy. In this comprehensive approach, an accurate air resistance term, due to mechanical losses in the air gap, slot, and holes, is also provided.

4.2 Škvor/Starr Approach

The squeeze-film damping effect – viscous loss that is associated with the squeezing of the fluid out from between two moving surfaces – dominates the dissipation mechanism for an air gap of several microns. For two parallel plates, the equivalent mechanical resistance due to squeeze-film damping (Starr [14]) is given by

$$R_{airgap} = \frac{3\mu}{2\pi h^3} \quad [\text{Ns/m}^5] \quad (4.1)$$

where μ is the absolute viscosity of air ($17.9 \times 10^{-6} \text{ Ns/m}^2$) and h is the unpolarized air gap thickness.

When holes are perforated in one of the plates, the viscous damping effect can be reduced significantly as air has an alternative path to escape through the holes rather

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than being squeezed out from the edge of the plate. The equivalent mechanical resistance due to holes (Škvor [15]) is given by

$$R_{hole} = \frac{12\mu}{N\pi h^3} G(A) \quad [\text{Ns/m}^5] \quad (4.2)$$

where N is the number of holes in the plate and $G(A)$ is given by

$$G(A) = \left[\frac{A}{2} - \frac{A^2}{8} - \frac{\ln A}{4} - \frac{3}{8} \right] \quad (4.3)$$

where A is the fraction of open area in the plate. Notice how Eqs. (4.1) and (4.2) are heavily dependent on h and independent of frequency. For many applications, a good approximation of the total resultant mechanical resistance is obtained by the parallel combination of Eqs. (4.1) and (4.2).

4.3 Deficiency of the Škvor/Starr Approach

The approach of Škvor/Starr is relatively straightforward and places a huge emphasis on the air gap thickness (the equivalent mechanical resistance is inversely proportional to it to the power of three). The next important parameter is the percentage of open area of holes in the backplate. Thus, the Škvor/Starr approach is based on the concepts of air gap thickness and total hole area, and does not take into any considerations of the location effect of holes on the mechanical resistance. However, in the analysis of Zuckerwar, the excited diaphragm deflection profile is taken into consideration by the use of a simple trial function for the diaphragm displacement and the extensive usage of Bessel functions in his formulation. Beside the air gap thickness and total hole area, he also emphasizes the dependency of the air resistance on the location of holes, backplate thickness, and backchamber volume. The omission of the

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above-mentioned parameters in the analysis of Škvor/Starr probably explains why the approach of Zuckerwar yields a lower, but more accurate, air resistance value than that of Škvor/Starr, a conclusion with which Gabrielson agreed.

In this chapter, the effect of the hole location on the mechanical-thermal noise is addressed by making modifications to the mechanical resistance expression of Škvor. A mode shape factor, which is based on the zero order Bessel and modified Bessel functions, is included in Škvor's formulation to consider the effect of the hole location in the backplate. With reference to two B&K microphones, the modified Škvor/Starr approach is compared with other approaches and experimental data.

4.4 Modified Škvor/Starr Approach

Figure 4.1 illustrates the nomenclature of a condenser microphone with a circular diaphragm of radius a . For two parallel plates, the equivalent mechanical resistance due to squeeze-film damping [14] is given by

$$R_{airgap} = \frac{3\mu}{2\pi h^3} \quad [\text{Ns/m}^5] \quad (4.1)$$

where μ is the absolute viscosity of air ($17.9 \times 10^{-6} \text{ Ns/m}^2$) and h is the unpolarized air gap thickness.

The lowest mode shape for a clamped circular diaphragm plate, normalized to a unit displacement at the center of the plate [19], can be given by

$$W(a_r) = 0.947J_0(ka_r) + 0.053I_0(ka_r) \quad (4.4)$$

where a_r is the radius ring that defines the location of holes in the backplate, $k=3.197/a$ for the clamped circular diaphragm plate [19], and $J_0(ka_r)$ and $I_0(ka_r)$ are the zero order Bessel and modified Bessel functions of ka_r , respectively. For regular non-circular

Fig. 4.1. Nomenclature of a condenser microphone: (a) Cross-sectional view and (b) arrangement of acoustic slot and holes in the backplate.

diaphragm shapes, such as square, hexagon, and octagon, they can be approximated by a circular one with an equivalent radius, having the same resonant frequency. It must be pointed out that the application of the polarization voltage induces a static deflection of the diaphragm, which, when combined with an incident pressure, alters the deflection mode. As such, for small air gap transducers, this can have a significant impact on the damping characteristics and must be taken into consideration. Figure 4.2 illustrates the normalized mode shape amplitude for the clamped circular diaphragm plate.

Fig. 4.2. Normalized mode shape amplitude for the clamped circular diaphragm plate versus normalized radius a_r/a .

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When holes are perforated in the backplate, the viscous damping can be reduced significantly as air has an alternative path to escape through the holes rather than being squeezed out from the edge of the backplate. In this case, the mechanical resistance corresponding to a single hole [15] is given by

$$R_{hole} = \frac{12\mu}{\pi h^3} G(A) \quad [\text{Ns/m}^5] \quad (4.5)$$

where $G(A)$ is given by

$$G(A) = \left[\frac{A}{2} - \frac{A^2}{8} - \frac{\ln A}{4} - \frac{3}{8} \right] \quad (4.3)$$

where A is the ratio of the area of each hole to the area of the diaphragm corresponding to it. The diaphragm area that corresponds to each hole can be approximated by dividing the total diaphragm area by the total number of holes in the backplate. However, for surface-micromachined planar microstructures, in which the air gap and backplate thicknesses are comparable, the hole resistance [20] must be considered and added to the squeeze-film damping. For N perforated holes arranged in a single radius ring, the mechanical resistance can be given by

$$R_{perf} = \frac{R_{hole}}{N} W^2(a_r) \quad [\text{Ns/m}^5] \quad (4.6)$$

where $W(a_r)$ is the mode shape factor that describes the hole location in the backplate [21]. A smaller radius ring, with a corresponding larger mode shape factor, has more dominant contribution to the total equivalent mechanical resistance as compared to a bigger one, with a corresponding smaller mode shape factor, because the diaphragm velocity decreases away from the center of the diaphragm. For multiple radius rings of holes, the total mechanical resistance can be approximated by a parallel combination of

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the mechanical resistances that are due to each radius ring. Then, the total equivalent air resistance, which accounts for the viscous damping losses in the air gap, slot, and holes, can be approximated by the parallel combination of Eqs. (4.1) and (4.6).

Thus, the pressure spectral density of the mechanical-thermal noise [18] can be expressed by

$$P_f = \sqrt{4k_B T (R_{airgap} // R_{perf})} \quad (4.7)$$

where k_B is the Boltzmann constant (1.38×10^{-23} J/K) and T is the absolute temperature (K). The background noise pressure level (A -weighted) of the mechanical-thermal noise [18] can be expressed by

$$N_T = \sqrt{\int_{f_1}^{f_2} 4k_B T R A^2(f) df} \quad (4.8)$$

where R is the total equivalent air resistance of the air gap, slot, and holes, $A(f)$ is the function of the A -weighted filter, and f_1 and f_2 are 10 Hz and 20 kHz, respectively.

4.5 Results and Discussions

Table 4.I tabulates the parameters of the B&K 0.5-inch (Type 4134) microphone [16]. As listed in Table 4.I, there are a total of six holes arranged in a single radius ring in the backplate. Figure 4.3 and Table 4.II compare the air resistance and A -weighted mechanical-thermal noise of the B&K 0.5-inch (Type 4134) microphone from various sources. As illustrated in Fig. 4.3, the analysis of Zuckerwar reveals that the air resistance is weakly dependent on the frequency while both the Škvor/Starr and modified Škvor/Starr approaches are independent of frequency, which are evidenced from their formulations as described in Chapters 4.2 and 4.4. From Fig. 4.3 and Table 4.II, it is clear that the Škvor/Starr approach overestimates the air

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resistance and mechanical-thermal noise since it provides the highest air resistance ($2.07 \times 10^8 \text{ Ns/m}^5$) and mechanical-thermal noise (20.2 dB(A)) values among all data sources. However, on the other hand, the modified Škvor/Starr approach provides a mechanical-thermal noise value of 18.3 dB(A), which is much closer to the specification of 18.0 dB(A).

Table 4.I. Parameters of the B&K 0.5-inch (Type 4134) microphone.

Parameter	Symbol	Value
Diaphragm radius	a	4.45 mm
Backplate radius	b	3.61 mm
Backplate thickness (at hole location)	l	0.8 mm
Unpolarized air gap	h	20 μm
Acoustic holes		
Number of holes per radius ring	N	6
Location of radius ring	a_r	2 mm
Hole radius	r_h	0.51 mm

Table 4.II. Comparison of the air resistance and A -weighted mechanical-thermal noise for the B&K 0.5-inch (Type 4134) microphone.

Source	$R \text{ (x } 10^8 \text{ Ns/m}^5)$	Mechanical-thermal noise ^b [dB(A)]
Škvor/Starr [1]	2.07	20.2
Zuckerwar [16]	1.89 ^a	19.7
Tarnow [6]	1.54	18.9
Ngo [1]	1.25	18.0
Modified Škvor/Starr	1.35	18.3
Specification [22]	-	18.0

^aTheoretical value at 250 Hz.

^bCalculated using Eq. (7).

Fig. 4.3. Comparison of approaches for the computation of air resistance versus frequency for the B&K 0.5-inch (Type 4134) microphone.

Table 4.III tabulates the parameters of the B&K MEMS microphone [23]. As listed in Table 4.III, four holes are arranged in a single radius ring in the backplate. Figure 4.4 and Table 4.IV compare the air resistance and *A*-weighted mechanical-thermal noise of the B&K MEMS microphone. Similarly, as illustrated in Fig. 4.4, the approach of Zuckerwar reveals the weak dependence of the air resistance on the frequency. From Fig. 4.4 and Table 4.IV, it is again clear that the Škvor/Starr approach overestimates the air resistance and mechanical-thermal noise as it provides the highest air resistance ($7.25 \times 10^8 \text{ Ns/m}^5$) and mechanical-thermal noise (25.6 dB(A)) values among all three approaches. Although the modified Škvor/Starr approach provides a

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smaller value for the air resistance ($6.69 \times 10^8 \text{ Ns/m}^5$) and mechanical-thermal noise (25.3 dB(A)), it is still not in good agreement with the measured value of 23.0 dB(A).

As described in Chapter 4.4, the diaphragm area that corresponds to each hole can be approximated by dividing the total diaphragm area by the total number of holes in the backplate. However, in the analysis of Škvor [15], the center of the hole coincides with that of the collecting diaphragm. As a result, an approximation of the diaphragm area by simply dividing the total diaphragm area by the total number of holes may not be feasible for some cases. For the B&K 0.5-inch (Type 4134) microphone, the radius ring is 2.0 mm, which is approximately half of the diaphragm radius. Thus, in this case, the diaphragm area approximation does have its validity, which is reflected by the good agreement of the calculated mechanical-thermal noise to its specification. For the case of highly perforated backplates [8–12] with a regular hole pitch, this diaphragm area approximation is also valid as the holes are evenly distributed throughout the backplate.

However, for the B&K MEMS microphone, the radius ring is 0.55 mm, which is less than one-third of the diaphragm radius. Therefore, in this case, the above-mentioned diaphragm area approximation does not have any creditability. As a result, an additional modification has to be made to A and $G(A)$. By equating the circumference of the radius ring to the diameter of four circles, the equivalent radius of the new collecting diaphragm can be estimated (i.e. $2\pi a_r = 4 \times 2r_e$, where r_e is the equivalent radius of the new collecting diaphragm (Fig. 4.5) and is used to approximate the new diaphragm area and subsequently, the new value of A). Thus, with a new $G(A)$ value of 0.819, the corrected air resistance is $5.65 \times 10^8 \text{ Ns/m}^5$ while the corrected

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mechanical-thermal noise is 24.5 dB(A), which is in much better agreement with the measured value of 23.0 dB(A) than before. By using the diameter approximation approach, the error between the theoretical and measured mechanical-thermal noise reduces from 10% to 6.5%.

Table 4.III. Parameters of the B&K MEMS microphone.

Parameter	Symbol	Value
Diaphragm radius	a	1.95 mm
Backplate radius	b	1.4 mm
Backplate thickness	l	150 μm
Unpolarized air gap	h	20 μm
Acoustic holes		
Number of holes per radius ring	N	4
Location of radius ring	a_r	0.55 mm
Hole radius	r_h	40 μm

Table 4.IV. Comparison of the air resistance and A -weighted mechanical-thermal noise for the B&K MEMS microphone.

Source	R ($\times 10^8 \text{ N s/m}^5$)	Mechanical-thermal noise ^b [dB(A)]
Škvor/Starr [18]	7.25	25.6
Zuckerwar [18]	3.22 ^a	23.0
Modified Škvor/Starr (Area approximation)	6.69	25.3
Modified Škvor/Starr (Diameter approximation)	5.65	24.5
Measurement [23]	-	23.0

^aTheoretical value at 250 Hz.

^bCalculated using Eq. (7).

Fig. 4.4. Comparison of approaches for the computation of air resistance versus frequency for the B&K MEMS microphone.

Fig. 4.5 A diagram illustrating the approximation of the equivalent radius (r_e) of the new collecting diaphragm.

4.6 Conclusion

The mechanical-thermal noise, together with other sources of background noise, determines the lowest limit of acoustic pressure that can be picked up by a microphone. The simple Škvor/Starr approach is commonly used to determine the mechanical-thermal noise performance of a condenser microphone but it does not consider the location effect of holes in the backplate. To address the hole location effect, a modified form of the Škvor/Starr approach has been proposed, in which a Bessel function-based mode shape factor is incorporated into the mechanical resistance expression of Škvor. With reference to two B&K microphones, the theoretical results obtained by the modified Škvor/Starr approach are in good agreements with those reported experimental ones. This proposed approach can be used to provide a quick and accurate assessment of the mechanical resistance of electrostatic transducers with perforated plate structures.

References

- [1] T. B. Gabrielson, "Mechanical-thermal noise in micromachined acoustic and vibration sensors," *IEEE Trans. Electron Devices*, vol. 40, pp. 903–909 (1993).
- [2] T. B. Gabrielson, "Fundamental noise limits in miniature acoustic and vibration sensors," *J. Vib. Acoust.*, vol. 117, pp. 405–410 (1995).
- [3] A. J. Zuckerwar, T. R. Kuhn and R. M. Serbyn, "Background noise in piezoresistive, electret condenser and ceramic microphones," *J. Acoust. Soc. Am.*, vol. 113, pp. 3179–3187 (2003).
- [4] A. J. Zuckerwar and K. C. T. Ngo, "Measured $1/f$ noise in the membrane motion of condenser microphones," *J. Acoust. Soc. Am.*, vol. 95, pp. 1419–1425 (1994).
- [5] V. Tarnow, "Thermal noise in microphones and preamplifiers," *B&K Technical Review*, vol. 3, pp. 3–14 (1972).
- [6] V. Tarnow, "The lower limit of detectable sound pressure," *J. Acoust. Soc. Am.*, vol. 82, pp. 379–381 (1987).
- [7] C. W. Tan, Z. H. Wang, J. M. Miao and X. F. Chen, "A study on the viscous damping effect for diaphragm-based acoustic MEMS applications," *J. Micromech. Microeng.*, vol. 17, pp. 2253–2263 (2007).
- [8] P. R. Scheeper, A. G. H. van der Donk, W. Olthuis and P. Bergveld, "Fabrication of silicon condenser microphones using single wafer technology," *J. Microelectromech. Syst.*, vol. 1, pp. 147–154 (1992).
- [9] W. Kühnel and G. Hess, "A silicon condenser microphone with structured backplate and silicon nitride membrane," *Sens. Actuators A*, vol. 30, pp. 251–258 (1992).
- [10] P. R. Scheeper, W. Olthuis and P. Bergveld, "Improvement of the performance of microphones with a silicon nitride diaphragm and backplate," *Sens. Actuators A*, vol. 40, pp. 179–186 (1994).
- [11] M. Pedersen, W. Olthuis and P. Bergveld, "A silicon condenser microphone with polyimide diaphragm and backplate," *Sens. Actuators A*, vol. 63, pp. 97–104 (1997).
- [12] M. Pedersen, W. Olthuis and P. Bergveld, "High-performance condenser microphone with fully integrated CMOS amplifier and DC-DC voltage converter," *J. Microelectromech. Syst.*, vol. 7, pp. 387–394 (1998).

Chapter 4 Modified Škvor/Starr Approach in Mechanical Resistance Analysis

- [13] R. Nadal-Guardia, A. M. Brosa and A. Dehé, "AC transfer function of electrostatic capacitive sensors based on the 1-D equivalent model: Application to silicon microphones," *J. Microelectromech. Syst.*, vol. 12, pp. 972–978 (2003).
- [14] J. B. Starr, "Squeeze-film damping in solid-state accelerometers," *IEEE Workshop in Solid-State Sensor and Actuator 4th Technical Digest*, pp. 44–47 (1990).
- [15] Z. Škvor, "On the acoustical resistance due to viscous losses in the air gap of electrostatic transducers," *Acustica*, vol. 19, pp. 295–299 (1967).
- [16] A. J. Zuckerwar, "Theoretical response of condenser microphones," *J. Acoust. Soc. Am.*, vol. 64, pp. 1278–1285 (1978).
- [17] A. J. Zuckerwar, "Principles of operation of condenser microphones," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.
- [18] C. W. Tan and J. M. Miao, "Analytical modeling for bulk-micromachined condenser microphone", *J. Acoust. Soc. Am.*, vol. 120, pp. 750–761 (2006).
- [19] A. W. Leissa, *Vibration of Plates* (AIP Press, New York, 1993).
- [20] D. Homentcovschi and R. N. Miles, "Modeling of viscous damping of perforated planar microstructures. Applications in acoustics", *J. Acoust. Soc. Am.*, vol. 116, pp. 2939–2947 (2004).
- [21] I. J. Oppenheim, A. Jain and D. W. Greve, "Electrical characterization of coupled and uncoupled MEMS ultrasonic transducers," *IEEE Trans. Ultrason., Ferroelect., Freq. Contr.*, vol. 50, pp. 297–304 (2003).
- [22] George S. K. Wong, "Microphone Data Applications," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 19, pp. 291–301.
- [23] P. R. Scheeper, B. Nordstrand, J. O. Gulløv, B. Liu, T. Clausen, L. Midjord and T. Storgaard-Larsen, "A new measurement microphone based on MEMS technology," *J. Microelectromech. Syst.*, vol. 12, pp. 880–891 (2003).

Chapter 5

Optimization of Viscous Damping: Application to PZT/Si Diaphragm-based Acoustic Transducer

5.1 Introduction

MEMS devices are in demand worldwide due to their miniaturized size, better performance, low cost and ease of fabrication. Despite the size advantage, one major challenge emerges during the design of MEMS devices, which involves the optimization of the damping characteristics of the mechanical structures. The resonant frequency of the mechanical structures is governed by their dimensions and material properties (i.e. mass and stiffness at supports) while the damping characteristics are predominantly governed by the structural configurations (i.e. cantilever, bridge, diaphragm or comb-drive) and the surrounding air medium.

Due to the nature of piezoelectricity, a piezoelectric-based diaphragm can function either as a microphone [1–4], a speaker [5, 6] or both [4, 7]. In Ref. [4], a micromachined ZnO diaphragm-based acoustic transducer is fabricated in which, the first resonant frequency occurs at 7.3 kHz. This limits the working bandwidth of the transducer, which falls short of the human audio frequency (20 Hz to 20 kHz). Very often, for piezoelectric acoustic transducers, the very low level of damping is provided by the packaging, if any, that houses the transducers [6]. A flat frequency response is desired so that the audio sound can be reproduced accurately.

To overcome this limited bandwidth issue, viscous damping can be introduced or increased in the transducer to eliminate the resonant peak and widen the working

bandwidth. One of the simplest ways to increase the viscous damping effect is to attach an optimized backplate to the diaphragm structure. As the diaphragm vibrates in response to the incident pressure wave, it expands and compresses the underlying air layer in the gap between itself and the backplate. This creates a reaction pressure that opposes the motion of the diaphragm. This reaction pressure generates an airflow, which introduces damping primarily in the air gap and in the openings (slot and holes) in the backplate. As a result, the new proposed structure resembles that of a condenser microphone. Thus, with an attached backplate, the condenser microphone theory can be used to model and design the mechanical response of the piezoelectric transducer.

Another transducer to benefit from a wider bandwidth in the frequency response characteristics is the piezoelectric ultrasonic transducer. It has been widely used to measure the distance accurately for robot and vehicle guidance. The ultrasonic transducer emits an ultrasonic beam with high directionality for good target acquisition. The transducer generates and transmits a burst signal with a certain frequency through air and the returned echo, after reflecting from a surface, is received by the same transducer. Usually, the working frequency of the transducer is similar for both the transmitting and the receiving modes. However, a moving target can shift the frequency of the reflected signal. If the bandwidth of the transducer is narrow, this frequency shift can reduce the receiving sensitivity of the transducer to the reflected signal. As such, an ultrasonic transducer with a wider bandwidth is highly desired.

In this chapter, it is demonstrated experimentally that the resonant peak of a lead zirconate titanate/silicon (PZT/Si) diaphragm at 15.6 kHz disappears and a widened bandwidth is observed when a silicon backplate, with four slots and four

holes, is attached to the diaphragm structure. Due to fabrication considerations, the slot around the circumference of the backplate is divided into four shorter slots. The parameters of the perforated slots and holes in the backplate are optimized using Zuckerwar's modeling approach [8, 9].

5.2 Fabrication of the PZT/Si Diaphragm

To demonstrate the effect of viscous damping, a PZT/Si-based diaphragm ($\varnothing$ 6.6 mm) is fabricated, which has a resonant frequency around 15.6 kHz. The key fabrication steps for the PZT/Si diaphragm include a low-temperature bonding technique using a spin-on polymer (Cytop polymer from AGC, Japan) [10], integration of the PZT layer on the silicon substrate, formation of electrode interconnect and pads, chemical mechanical polishing (CMP) to thin down the bulk PZT plate, wet etching of the PZT layer and release of the diaphragm structure by using the DRIE technique [11–13].

Figure 5.1 illustrates the fabrication sequence [14] of the PZT/Si diaphragm. Firstly, a Cr (10 nm)/Au (300 nm) electrode layer is sputtered on the device layer (20 μm) of a 4-inch SOI wafer and one side of a 90-mm PZT plate (300 μm). These metallic layers form the bottom electrode of the PZT/Si diaphragm. A layer of the Cytop polymer is spin-coated and patterned on the SOI wafer. The through-hole in the Cytop polymer layer is then filled by sputtering another layer of Cr (10 nm)/Au (300 nm) for the purpose of electrical interconnection (Fig. 5.1(a)). Secondly, the SOI wafer and the bulk PZT plate are bonded together thermally at 160 $^{\circ}\text{C}$ at a pressure of 3 bar for 30 min (Fig. 5.1(b)). Thirdly, the thick bulk PZT plate is thinned down to a

Fig. 5.1. Fabrication sequence of the PZT/Si-based diaphragm: (a) Sputtering of Cr/Au on both PZT plate and SOI wafer, (b) thermal polymer bonding of PZT plate and SOI wafer, (c) thinning of PZT plate by using CMP and (d) sputtering of top Cr/Au electrode on the PZT layer and deep etching on the backside of SOI wafer to release the diaphragm.

thickness of about 27 μm using CMP (Fig. 5.1(c)). In the fourth step, a layer of Cr (10 nm)/Au (300 nm) is sputtered on the PZT layer to form the top electrode layer. The wet etching of PZT is performed alternatively in diluted HCl:HF and HNO_3 solutions [15] with a photoresist mask to open a window to access the bottom electrode (not illustrated in Fig. 5.1(d)). After that, the backside handle layer (450 μm) of the SOI wafer is etched using DRIE until the embedded SiO_2 etch-stop layer to release the PZT/Si diaphragm (Fig. 5.1(d)). Finally, the device wafer is then diced to release the individual PZT/Si diaphragm die.

5.3 Design Consideration of the backplate

With an attached Si backplate, the condenser microphone theory developed by Zuckerwar can be used to model and design the mechanical response of the PZT/Si acoustic transducer. The mechanical sensitivity [8, 9] of the condenser microphone can be expressed by

$$M_m(\omega) = \frac{y(r)}{p_i} = \frac{1}{TK^2} \cdot \frac{J_2(Ka)}{J_0(Ka) + D(\omega)} \quad (5.1)$$

where $y(r)$ is the diaphragm deflection, p_i is the incident pressure, T is the diaphragm tension, a is the diaphragm radius, ω is the angular frequency of the incident pressure, and $J_0(Ka)$ and $J_2(Ka)$ are the zero and second order Bessel functions of Ka , respectively, while K , which is the wave number of sound in the diaphragm, and $D(\omega)$, which is the air layer damping term (a dimensionless quantity), can be expressed, respectively, by

$$K = \omega \left(\frac{\sigma_M}{T} \right)^{1/2} \quad (5.2)$$

where σ_M is the mass surface density of the diaphragm and

$$D(\omega) = \frac{j4\omega\rho_o J_o(Ka)}{\pi T J_2(Ka)} \sum_{m=0}^{\infty} \left[-j\omega \left(\frac{y_m}{y_o} \right) + \sum_{s=1}^q \left(\frac{f_s}{y_o} \right) s_s \Gamma_m J_o(\xi_m a_s) \right] \quad (5.3)$$

$$\times \frac{1}{T_m J_0(\xi_m a)} \left\{ \frac{J_2(Ka) \delta(m)}{4K^2 a^2} + \frac{J_1(Ka) [1 - \delta(m)]}{2Ka (K^2 a^2 - \xi_m^2 a^2)} \right\}$$

where ρ_o is the static air density (1.205 kg/m³) and $J_l(Ka)$ is the first order Bessel function of Ka . For further reading, the detailed mathematical derivation of $D(\omega)$ can be found in Refs. 8 and 9.

The frequency dependent air resistance R_A [8, 9], which is due to the viscous damping losses in the air gap, slots and holes, accounts for the mechanical-thermal noise and it can be expressed by

$$R_A(\omega) = \frac{8\pi T \text{Im}(D)}{\omega(\pi a^2)^2} \quad (5.4)$$

where $\text{Im}(D)$ is the imaginary part of $D(\omega)$. Then, the background noise pressure level (A -weighted) of the mechanical-thermal noise [16] can be expressed by

$$N_T = \sqrt{\int_{f_1}^{f_2} 4k_B T R_A(f) A^2(f) df} \quad (5.5)$$

where k_B is the Boltzmann constant (1.38 x 10⁻²³ J/K), T is the absolute temperature (K), $A(f)$ is the function of the A -weighted filter, and f_1 and f_2 are 10 Hz and 20 kHz, respectively. Therefore, the mechanical-thermal noise of the microphone is dependent on the amount of viscous damping that is present in the mechanical structure.

Figure 5.2 illustrates the experimental vibration magnitude plot of the PZT/Si diaphragm without the backplate. To determine the tension of the PZT/Si diaphragm, the theoretical result of Zuckerwar (Eq. (5.1)) is curve-fitted to the experimental result

of the PZT/Si diaphragm at the first and second resonant frequencies around 16 kHz and 40 kHz, respectively. As such, the PZT/Si diaphragm tension works out to be approximately 5200 N/m. With preliminary simulation results, it is decided that four slots and four holes (in a single row) will be implemented in the backplate design. With the diaphragm tension value and the optimized location of the slots and holes in the backplate, the frequency response plots of the PZT/Si diaphragm with a backplate are generated in Fig. 5.2 for air gap thicknesses ranging from 10 to 16 μm . Due to space constraint, the frequency response data of 11, 13, and 15 μm air gap thickness are omitted without compromising any data integrity since an obvious trend is developing in the plot with the presented data. Figures 5.3 and 5.4 illustrate the effect of the air gap thickness on the bandwidth and the *A*-weighted mechanical-thermal noise, respectively.

Fig. 5.2. Experimental vibration magnitude plot of the PZT diaphragm without the backplate and theoretical frequency response plots of the PZT diaphragm with backplates of varying air gap thicknesses (10 to 16 μm).

Fig. 5.3. Effect of the air gap thickness on the bandwidth.

Fig. 5.4. Effect of the air gap thickness on the *A*-weighted mechanical-thermal noise.

As illustrated in Fig. 5.3, the best choice of air gap thickness is between 11 to 13 μm , which yield a bandwidth from 14 to 16 kHz. As illustrated in Fig. 5.4, the best choice of air gap thickness is 12 μm and beyond, which yield an *A*-weighted mechanical-thermal noise of 28.6 dB(A) and below. Therefore, as clearly seen from both Figs. 5.3 and 5.4, the optimum choice of air gap thickness is 12 and 13 μm .

5.4 Fabrication of the PZT/Si Acoustic Transducer

Figure 5.5 illustrates the mechanical structure of the silicon backplate. Two opposite corners of the backplate are removed by etching so that the electrode pads of the PZT/Si diaphragm can be exposed for wiring connections. There are a total of three fabrication steps for the silicon backplate. Firstly, a layer of photoresist and bonding polymer is spin-coated and then patterned on the top side of a 300- μm thick wafer. Secondly, an etching step is performed to form a 10- μm deep air cavity in the wafer. Finally, the bottom side of the wafer is patterned and then deeply etched to form the slots (0.6 mm width) and holes ($\varnothing$ 80 μm and located about 1 mm from the center of the backplate) and to release the individual backplate structure. After that, the PZT/Si diaphragm and the backplate are thermally bonded together at a temperature of 160 $^{\circ}\text{C}$ for 30 min. After bonding, the air gap between the PZT/Si diaphragm and the backplate is approximately 12 μm (as determined above). Finally, the bonded device is wired to a PCB board to facilitate the experimental measurements. Figure 5.6 illustrates the fabricated acoustic transducer at various stages of the fabrication process.

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Fig. 5.5. Fabricated acoustic transducer: (a) Cross-sectional view and (b) top view showing the arrangement of acoustic slots and holes in the backplate.

(a)

(b)

(c)

Fig. 5.6. Acoustic transducer at various stages of the fabrication process: (a) PZT diaphragm with electrode pads, (b) silicon backplate with slots and holes, and (c) bonded device mounted on PCB board with attached wirings.

5.5 Transducer Characterization

For the purpose of transducer characterization in both the speaker and the microphone modes, an acoustical measurement setup (Fig. 5.7) is used, which consists of an acoustic box with a foam-lining interior that houses the speaker, reference microphone and device under test (DUT), personal computer with signal generating and frequency analyzing capabilities, B&K Type 2706 power amplifier for driving the speaker, microphone preamplifier for the reference microphone and HP 35670A dynamic signal analyzer for the DUT.

Fig. 5.7. An acoustical measurement setup for transducer characterization.

5.5.1 Speaker Mode

Figure 5.8 illustrates the output SPL of the PZT/Si diaphragm with and without the backplate when it is in the speaker mode. Without the backplate, at a distance of 30 mm, the maximum SPL measured is 110 dB (ref. 20 μ Pa) at its resonant frequency of 15.6 kHz. However, with the backplate, the measured maximum SPL attenuates to around 85 dB, which is a significant drop of 25 dB, while its bandwidth widens considerably from 13 to 18 kHz, which is an enhancement of 5 kHz. With an attached backplate, the viscous damping effect due to the small air gap is introduced into the mechanical structure to alter its damping characteristics. In conjunction with four slots and four position-optimized holes in the backplate, the frequency response of the PZT/Si diaphragm is enhanced with a wider bandwidth since the resonant peak is eliminated.

5.5.2 Microphone Mode

Figure 5.9 illustrates the frequency response characteristics (unamplified sensitivity) of the PZT/Si diaphragm with and without the backplate when it is in the microphone mode. Without the backplate, there is a sharp peak at its resonant frequency and the maximum recorded sensitivity is -40 dB (ref. 1 V/Pa). However, with the attached backplate, the frequency response of the PZT/Si diaphragm is relatively flatter and the maximum sensitivity is reduced to about -60 dB, which is a 20 dB drop in magnitude. Without the backplate, the presence of resonance limits the working bandwidth of the device. For the accurate reproduction of audio sound, a flat frequency response, without any resonance over the audio frequency range, is highly

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Fig. 5.8. Output SPL of the PZT/Si diaphragm with and without the backplate in the speaker mode.

Fig. 5.9. Frequency response plot of the PZT/Si diaphragm with and without the backplate in the microphone mode.

desirable. As evidenced in both Figs. 5.8 and 5.9, the noise signals around and below 5 kHz can be attributed to the acoustical measurement setup.

5.5.3 Noise Performance

The noise performance of the PZT/Si acoustic transducer (with backplate) is evaluated by measuring the acoustic signal well below the first resonant frequency of the PZT/Si diaphragm. Figure 5.10 illustrates the output voltage signal of the transducer in response to a 1 Pa sound pressure signal at 1 kHz frequency. The output voltage signal is monitored using HP 35670 dynamic signal analyzer. As clearly illustrated, within the whole audio frequency range, the average noise signal is approximately 0.15 μV while the peak voltage signal at 1 kHz is approximately 6.4 μV , which provides a signal-to-noise ratio of about 33 dB.

Fig. 5.10. Output voltage signal of the PZT/Si acoustic transducer (with backplate) in response to a 1 Pa sound pressure signal at 1 kHz.

5.5.4 Proposed Application

It has been successfully demonstrated that the attachment of an optimized silicon backplate to the PZT/Si diaphragm can modify its frequency response characteristics desirably in both the speaker and the microphone modes. In this respect, a proposed application of such a PZT/Si acoustic transducer is a two-in-one acoustic system that switches between the speaker and the microphone mode, in which only one person is allowed to talk at any time while the rest listens. As a result, the requirement to house two separate speaker and microphone units in an acoustic system can be eliminated, which can help to save precious real estates.

5.6 Conclusion

The viscous damping effect plays an important role in the design of MEMS devices as it dominates the dissipation mechanism of miniaturized mechanical structures. By attaching a silicon backplate with optimized air gap, slots and holes to a PZT/Si diaphragm, it has been demonstrated experimentally that its frequency response can be optimized desirably. The bandwidth, A -weighted mechanical-thermal noise and signal-to-noise ratio of the PZT/Si acoustic transducer are approximately 18 kHz, 28.6 dB(A) and 33 dB, respectively.

References

- [1] M. Royer, J. O. Holmen, M. A. Wurm and O. S. Aadland, “ZnO on Si integrated acoustic sensor,” *Sens. Actuators A*, vol. 4, pp. 357–362 (1983).
- [2] E. S. Kim and R. S. Muller, “IC-processed piezoelectric microphone,” *IEEE Electron Dev. Lett.*, vol. EDL-8, pp. 467–468 (1987).
- [3] R. P. Ried, E. S. Kim, D. M. Hong and R. S. Muller, “Piezoelectric microphone with on-chip CMOS circuits,” *J. Microelectromech. S.*, vol. 2, pp. 111–120 (1993).
- [4] S. C. Ko, Y. C. Kim, S. S. Lee, S. H. Choi and S. R. Kim, “Micromachined piezoelectric membrane acoustic device,” *Sens. Actuators A*, vol. 103, pp. 130–134 (2003).
- [5] C. H. Han and E. S. Kim, “Parylene-diaphragm piezoelectric acoustic transducers,” *Proc. 13th IEEE Int. Conf. on MEMS*, Miyazaki, Japan, pp. 148–152, 23–27 January 2000.
- [6] S. H. Yi and E. S. Kim, “Piezoelectric microspeaker with compressive nitride diaphragm,” *Proc. 15th IEEE Int. Conf. on MEMS*, Las Vegas, USA, pp. 260–263, 20–24 January 2002.
- [7] S. S. Lee, R. P. Ried and R. M. White, “Piezoelectric cantilever microphone and microspeaker,” *J. Microelectromech. Syst.*, vol. 5, pp. 238–242 (1996).
- [8] A. J. Zuckerwar, “Theoretical response of condenser microphones,” *J. Acoust. Soc. Am.*, vol. 64, pp. 1278–1285 (1978).
- [9] A. J. Zuckerwar, “Principles of operation of condenser microphones,” in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.
- [10] K. W. Oh, A. Han, S. Bhansali and C. H. Ahn, “A low-temperature bonding technique using spin-on fluorocarbon polymers to assemble microsystems,” *J. Micromech. Microeng.*, vol. 12, pp. 187–191 (2002).
- [11] J. X. Gao, L. P. Yeo, M. B. Chan-Park, J. M. Miao, Y. H. Yan, J. B. Sun, Y. C. Lam and C. Y. Yue, “Antistick postpassivation of high-aspect ratio silicon molds fabricated by deep-reactive ion etching,” *J. Microelectromech. Syst.*, vol. 15, pp. 84–93 (2006).

- [12] P. Dixit and J. M. Miao, "Aspect ratio dependent copper electrodeposition technique for very high aspect ratio through-hole plating," *J. Electrochem. Soc.*, pp. 153, G552–G559 (2006).
- [13] L. Fu, J. M. Miao, X. X. Li and R. M. Lin, "Study of deep silicon etching for micro-gyroscope fabrication," *Appl. Surf. Sci.*, vol. 177, pp. 78–84 (2001).
- [14] Z. H. Wang, J. M. Miao and C. W. Tan. "Acoustic transducers with a perforated damping backplate based on PZT/silicon wafer bonding technique," *Sens. Actuators A*, vol. 149, pp. 277–283 (2009).
- [15] K. Zheng, J. Lu and J. Chu, "A novel wet etching process of $\text{Pb}(\text{Zr,Ti})\text{O}_3$ thin films for applications in microelectromechanical system," *Jpn. J. Appl. Phys.*, vol. 43, pp. 3934–3937 (2004).
- [16] C. W. Tan and J. M. Miao, "Analytical modeling for bulk-micromachined condenser microphones," *J. Acoust. Soc. Am.*, vol. 120, pp. 750–761 (2006).

Chapter 6

Optimization of Viscous Damping: Application to Diaphragm-based RF MEMS Shunt Switch

6.1 Introduction

The RF MEMS switch can be classified into two categories: (1) series switch based on metal-metal contact (DC contact) [1–6] and (2) shunt switch based on metal-dielectric contact [7–11]. The DC contact series switch provides high isolation from 0.1 to 20 GHz while the capacitive shunt switch offers excellent isolation from 10 to 120 GHz with the help of a silicon oxide or nitride dielectric layer [12]. By eliminating the dielectric layer, one special type of metal-metal contact shunt switch [13, 14] can be developed, which can be used to replace the series switch. For a metal-metal contact shunt switch, there are two design challenges. Firstly, it concerns the packaging of the switch, in which exposure to air compromises the reliability and life span of the metal contacts. Secondly, since the switch is a mechanical actuating device with moving parts, there is a physical limitation on the maximum achievable switching speed.

To resolve the above-mentioned issues, a new RF MEMS shunt switch design [15] is proposed and developed, which features a low-stress corrugated diaphragm [16–20] and a perforated base substrate. Figure 6.1 illustrates the schematic diagram of the proposed RF MEMS shunt switch design. The switch design consists of three layers: (1) a top corrugated circular diaphragm layer incorporating a metal contact, (2) a middle perforated base substrate layer incorporating a coplanar waveguide (CPW) and (3) a bottom back support layer incorporating a cavity to function as an air reservoir.

Figure 6.1. A schematic diagram of the proposed 3-layer RF MEMS shunt switch.

The first issue of packaging is resolved as the metal-metal contact is encapsulated between the top diaphragm and middle base substrate layers of the device, which effectively eliminates the detrimental exposure to air. The second issue of switching speed performance is overcome as the perforations in the base substrate reduce the squeeze-film damping effect [21, 22] as air has an alternative path to escape through the holes into the air cavity reservoir in the bottom back support layer rather than being squeezed out from the edge of the base substrate.

6.2 Theoretical Consideration

The RF MEMS shunt switch works on the principle of electrostatic actuation. When an actuating voltage is applied across the diaphragm and the base substrate, the diaphragm is pulled downwards and makes physical contact with the base substrate as a result of the generated electrostatic force. With the help of a relay metal underneath the diaphragm, the snap-down action of the diaphragm establishes an electrical contact with the CPW. When the actuating voltage is removed, the diaphragm breaks the electrical contact with the CPW and springs back to its initial unactuated state. As the diaphragm moves up and down in response to the applied actuating voltage, it expands and compresses the underlying air layer in the air gap between itself and the base substrate. This creates a reaction pressure that opposes the motion of the diaphragm. This reaction pressure generates an airflow, which introduces damping primarily in the air gap and in the openings in the base substrate. The small air gap between the diaphragm and the base substrate leads to the squeeze-film damping effect. To enhance the switching speed of the RF MEMS shunt switch, strategically-placed holes, which

provide a large surface area for the viscous boundary layer damping, are perforated in the base substrate to mitigate the squeeze-film damping effect. An air cavity in the bottom back support layer serves as an air reservoir for the airflow through the hole openings in the base substrate.

6.2.1 Damping Coefficient

As clearly described, the mechanical structure of the RF MEMS shunt switch closely resembles that of the condenser microphone. Thus, the condenser microphone theory developed by Zuckerwar [23, 24] can be utilized to model and design the mechanical response of the RF MEMS shunt switch. In this perspective, only the air resistance expression is used in the theoretical consideration. Well below the first resonant frequency of the diaphragm, the air resistance [23, 24] can be expressed as

$$R_A(\omega) = \frac{8\pi T \text{Im}(D)}{\omega(\pi a^2)^2} \quad [\text{Ns/m}^5] \quad (6.1)$$

where T is the diaphragm tension, $\text{Im}(D)$ is the imaginary part of $D(\omega)$, ω is the angular velocity, a is the diaphragm radius and $D(\omega)$ is the air layer damping term (a dimensionless quantity), which can be expressed by

$$D(\omega) = \frac{j4\omega\rho_o J_o(Ka)}{\pi T J_2(Ka)} \sum_{m=0}^{\infty} \left[-j\omega \left(\frac{y_m}{y_o} \right) + \sum_{s=1}^q \left(\frac{f_s}{y_o} \right) s_s \Gamma_m J_o(\xi_m a_s) \right] \quad (6.2)$$

$$\times \frac{1}{T_m J_0(\xi_m a)} \left\{ \frac{J_2(Ka) \delta(m)}{4K^2 a^2} + \frac{J_1(Ka) [1 - \delta(m)]}{2Ka(K^2 a^2 - \xi_m^2 a^2)} \right\}$$

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where ρ_o is the static air density (1.205 kg/m^3) and $J_0(Ka)$, $J_1(Ka)$ and $J_2(Ka)$ are the zero, first and second order Bessel functions of Ka , respectively, while K , which is the wave number of sound in the diaphragm, can be expressed by

$$K = \omega \left(\frac{\sigma_M}{T} \right)^{1/2} \quad (6.3)$$

where σ_M is the mass surface density of the diaphragm. For further reading, the detailed mathematical derivation of $D(\omega)$ can be found in Refs. 23 and 24. Here, a new damping term, called damping coefficient, is introduced, which can be expressed as

$$B_a(\omega) = \frac{8\pi T \text{Im}(D)}{\omega} \quad [\text{Ns/m}] \quad (6.4)$$

6.2.2 Pull-in Voltage

The pull-in voltage [25] of the corrugated diaphragm can be expressed by

$$V_{pi} = \sqrt{\frac{6h^2}{5\epsilon_0} \left[\frac{7-\nu}{81(1-\nu)(1-\nu^2)} \left(\frac{Edh^3}{a^4} \right) + \frac{4}{9(1-\nu)(1-\nu^2)} \left(\frac{Ed^3h}{a^4} \right) + \frac{4}{3} \frac{\sigma_0 dh}{\eta a^2} \right]} \quad (6.5)$$

where h is the unpolarized air gap, ϵ_0 is the dielectric permittivity of free air ($8.85 \times 10^{-12} \text{ F/m}$), ν is the Poisson ratio, E is the Young's modulus, d is the diaphragm thickness, a is the diaphragm radius, σ_0 is the initial diaphragm stress (a measured value of 51 MPa is used), and η is the residual stress reduction ratio [18] due to the presence of corrugations, which can be expressed by

$$\eta = \frac{\sigma_0}{\sigma} = 1 + 6 \sin(\beta) \left(\frac{h_c}{d} \right)^2 \frac{N_c w_c}{a - N_c (w_c + b_c)} \quad (6.6)$$

where σ is the equilibrium diaphragm stress with corrugations, β is the profile angle of the corrugation, h_c is the corrugation depth, N_c is the number of corrugations, w_c is the

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width of the bottom corrugation, and b_c is the width of the top corrugation (Fig. 6.2). A low pull-in voltage can be realized by having a small air gap, thin diaphragm, large diaphragm and low diaphragm stress.

Fig. 6.2. Nomenclature of the corrugated diaphragm.

6.2.3 Switching Time

The switching time of the RF MEMS shunt switch can be estimated by using the modeling approach of Steenken *et al.* [25], which consists of both the pull-in and release motions of the diaphragm. For the pull-in motion to occur, the actuating voltage must be larger than the pull-in voltage of the diaphragm. Then, the switching time, from the up-position (y_1) to the down-position (y_0), can be expressed by

$$t_{pull-in}(\bar{y}) \Big|_{\bar{y}_1}^{\bar{y}_0} = \frac{-8B_d h^3}{A \varepsilon_0 V_{pi}^2 f_1(-\gamma) f_2(-\gamma) f_3(2\delta)} \times \left[f_3(2\delta) \ln(\bar{y} + \gamma) - \frac{f_1(-\gamma)}{3} \ln(f_2(\bar{y})) \right. \\ \left. + \frac{f_1(-\gamma) - 3f_3(2\delta)}{6} \ln(f_1(\bar{y})) - \frac{f_2(-\gamma)(f_2(-\delta\gamma) - 3\delta)}{\sqrt{3f_3(\delta)}} \arctan \frac{f_2(\bar{y}) - 3\delta}{\sqrt{3f_3(\delta)}} \right] \Big|_{\bar{y}_1}^{\bar{y}_0} \quad (6.7)$$

where $\bar{y}$ is the normalized position ($\bar{y}=z/h$), A is the diaphragm area, γ is the slip-flow constant ($\gamma = 9\lambda/h$), λ is the mean free path of the gas molecules (about 70 nm under atmospheric pressure at room temperature) and

$$\delta = \cosh \left[\frac{1}{3} \arccos h \left(\frac{2V^2}{V_{pi}^2} - 1 \right) \right] \quad (6.8)$$

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$$f_1(x) = f_2(x)[f_2(x) - 6\delta] + 3f_3(2\delta) \quad (6.9)$$

$$f_2(x) = 3x + 2\delta - 1 \quad (6.10)$$

$$f_3(x) = x^2 - 1 \quad (6.11)$$

For the release motion of the diaphragm, the switching time, from the down-position (y_0) to the up-position (y_1), can be expressed by

$$t_{\text{release}}(\bar{y})_{\bar{y}_0}^{\bar{y}_1} = \frac{-8B_a h^3}{27A\epsilon_0 V_{pi}^2 (1+\gamma)} \times \left[\frac{1+\gamma}{\bar{y}} + \ln\left(\frac{1}{\bar{y}} - 1\right) - \frac{1}{\gamma^2} \ln\left(1 + \frac{\gamma}{\bar{y}}\right) \right]_{\bar{y}_0}^{\bar{y}_1} \quad (6.12)$$

Thus, it can be seen that the switching time is strongly dependent on the damping coefficient, air gap and pull-in voltage. A fast switching time can be achieved from a small damping coefficient, thin air gap and large pull-in voltage. As such, the switching time can be enhanced by reducing the damping coefficient via the meticulous design of the air gap and the base substrate.

6.3 Design of the Perforated Base Substrate

In Chapter 3, a conclusion has been drawn that the main effects of air gap and slot and the slot/hole location interaction are found to have significant influences over the mechanical-thermal noise. The mechanical-thermal noise is dependent on the amount of viscous damping that is present in the mechanical structure, which is manifested in the form of losses in the air gap, slot and holes in the backplate of a condenser microphone. In this aspect, in order to enhance the mechanical performance of the RF MEMS shunt switch with respect to the switching speed, the damping coefficient is optimized with reference to two parameters: (1) air gap and (2) hole location. Table 6.I tabulates the parameters of the RF MEMS shunt switch.

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Table 6.I. Parameters of the RF MEMS shunt switch.

Parameter	Symbol	Value
Diaphragm:		
Radius	a	1.75 mm
Thickness	d	1.75 μm
Mass surface density	σ_M	0.0058 kg/m ²
Tension	T	87.5 N/m
Actuation voltage ^a	V	60 V
Base substrate:		
Radius	b	1.75 mm
Unpolarized air gap ^a	h	8 μm
Number of holes per radius ring	b_l	10
Location of radius ring ^a	a_l	450, 620, 790, 960, 1130, 1300, 1470 μm
Hole radius	r_l	50 μm
Hole depth	l_l	500 μm
Back support:		
Cavity volume	V_C	1.443 x 10 ⁻⁹ m ³

^aSelected parameter after optimization.

6.3.1 Optimization of Air Gap

Figure 6.3 illustrates the damping coefficient plot of the RF MEMS shunt switch with and without perforation scheme in the base substrate for various air gap thicknesses. From Fig. 6.3, the damping coefficients are independent of the switching speed up to a few kHz. Figure 6.4 illustrates the effect of the air gap thickness on the damping coefficient of the RF MEMS switch with and without perforation scheme in the base substrate at 100 Hz switching speed. As illustrated, with and without perforations, the damping coefficients show a decrease with increasing air gap thicknesses. For all air gap thicknesses, with perforations, the damping coefficients are reduced significantly in excess of 13 times when comparisons are made with those without any perforations.

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Figure 6.3. Damping coefficient plot of the RF MEMS shunt switch with and without perforations in the base substrate for various air gap thicknesses.

Figure 6.4. Effect of the air gap thickness on the damping coefficient of the RF MEMS shunt switch with and without perforations in the base substrate at 100 Hz switching speed.

Figure 6.5 illustrates the effect of the air gap thickness on the actuation voltage of the RF MEMS shunt switch. The actuation voltage is calculated at 110% of the pull-in voltage of the diaphragm. As illustrated in Fig. 6.5, the actuation voltage shows an increase with increasing air gap thickness. As clearly seen from both Figs. 6.4 and 6.5, the best choice of air gap thickness is 8 μm . Although an air gap thickness of 10 μm presents a lower damping coefficient than that of 8 μm , it compromises on its actuation voltage in that the actuation voltage of 85 V is rather on the high side and not as manageable as that (60 V) at 8 μm air gap.

Figure 6.5. Effect of the air gap thickness on the actuation voltage of the RF MEMS shunt switch.

6.3.2 Optimization of Hole Location

Figure 6.6 illustrates the damping coefficient plot of the RF MEMS shunt switch for various perforation arrangements in the base substrate. From Fig. 6.6, the damping coefficients are almost independent of the switching speed up to 15 kHz. The three perforation arrangements of the base substrate, which consist of seven radius ring locations each, are described in Table 6.II. From Table 6.II, for the first and second arrangements, the radius rings are separated by a distance of 170 μm . For the third arrangement, the radius rings are separated by a distance of 150 μm . For the first arrangement, the radius rings are separated by a distance of 150 μm . For the first arrangement, the perforations are arranged in such a way that they are closest to the center of the base substrate. For the third arrangement, the perforations are arranged furthest away from the center of the base substrate.

Figure 6.6. Damping coefficient plot of the RF MEMS shunt switch for various perforation arrangements in the base substrate.

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Figure 6.7 illustrates the effect of the perforation arrangement (or hole location) on the damping coefficient of the RF MEMS shunt switch at 100 Hz switching speed. From Fig. 6.7, the first arrangement provides the lowest damping coefficient of all three arrangements. As such, the first arrangement of perforations is adopted for the design of the base substrate for the RF MEMS switch.

Table 6.II. Parameters of the three perforation arrangements in the base substrate.

Arrangement	Location of radius ring (μm)
1	450, 620, 790, 960, 1130, 1300, 1470
2	620, 790, 960, 1130, 1300, 1470, 1640
3	740, 890, 1040, 1190, 1340, 1490, 1640

Figure 6.7. Effect of the perforation arrangement (or hole location) on the damping coefficient of the RF MEMS shunt switch at 100 Hz switching speed.

6.4 Device Fabrication

The key fabrication steps for the RF MEMS shunt switch include a low-temperature bonding technique using a spin-on polymer (Cytop polymer from AGC, Japan), fabrication of the corrugated diaphragm and the DRIE technique to release the diaphragm. During the fabrication process, three silicon wafers, which consist of the diaphragm, base substrate and back support wafers, are fabricated separately, after which they are bonded thermally together at a low temperature to realize a wafer-level encapsulated RF MEMS switch.

Figure 6.8 illustrates the fabrication sequence [15] of the RF MEMS shunt switch. For the fabrication of the bottom back support layer, there are a total of two steps. Firstly, a layer of photoresist and Cytop polymer (2 μm) is spin-coated and patterned on the top side of a 4-inch silicon wafer (350 μm) (Fig. 6.8(a)). Secondly, using DRIE etching with the photoresist as a mask, a circular cavity of 150 μm depth is etched in the silicon wafer (Fig. 6.8(b)).

For the fabrication of the middle perforated base substrate layer, there are a total of four steps. Firstly, a layer of SiO_2 (1.8 μm) is oxidized on both sides of a 4-inch silicon wafer (500 μm thickness and 5000 $\Omega\cdot\text{cm}$ resistivity). A CPW transmission line is then formed by metal deposition and lift-off process (Fig. 6.8(c)). Secondly, a layer of Cytop polymer (2 μm) is spin-coated and patterned on the metal layer (Fig. 6.8(d)). Thirdly, a layer of photoresist is spin-coated and patterned. Using the photoresist layer as a mask, the SiO_2 layer is removed by using the CF_4/O_2 plasma-based RIE etching (Fig. 6.8(e)). Finally, using the same photoresist mask, through-holes are etched in the

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base substrate by using the DRIE etching process (Fig. 6.8(f)). A SEM picture of the fabricated base substrate with perforations is illustrated in Fig. 6.9.

For the fabrication of the top corrugated diaphragm layer, there are a total of four steps. Firstly, a circular recess of 6 μm depth is etched into the top side of a 4-inch silicon wafer (200 μm) by the DRIE etching process (Fig. 6.8(g)). Secondly, using an optimized DRIE process recipe, slightly positive-tapered corrugations are etched into the silicon wafer (Fig. 6.8(h)). Thirdly, a layer of SiO_2 (100 nm) is deposited, which is followed by the deposition of a LPCVD Si_3N_4 layer (1.2 μm). This $\text{SiO}_2/\text{Si}_3\text{N}_4$ thin film layer constitutes the low-stress corrugated diaphragm of the RF MEMS shunt switch. The $\text{SiO}_2/\text{Si}_3\text{N}_4$ layer is patterned using a photoresist mask by RIE etching (Fig. 6.8(i)). Finally, a contact metal for the DC contact is deposited onto the $\text{SiO}_2/\text{Si}_3\text{N}_4$ layer by sputtering and lift-off process (Fig. 6.8(j)).

For the bonding of the three wafers, there are a total of three steps. Firstly, the diaphragm, base substrate and back support wafers are aligned and then thermally bonded together at a temperature of 160 $^\circ\text{C}$ using the Cytop polymer as the bonding layer (Fig. 6.8(k)). Secondly, the $\text{SiO}_2/\text{Si}_3\text{N}_4$ layer on the top diaphragm wafer is removed by RIE etching (Fig. 6.8(l)). Thirdly, using a photoresist mask, the $\text{SiO}_2/\text{Si}_3\text{N}_4$ diaphragm is released by using DRIE etching (Fig. 6.8(m)). Finally, an electrode layer of Ti/Au is sputtered onto the diaphragm via a shadow mask (Fig. 6.8(n)). Figure 6.10 illustrates a SEM picture (top view) of the fabricated RF MEMS shunt switch.

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(a) Patterning of Cytosol polymer and photoresist.

(b) DRIE etching of cavity and stripping of photoresist.

(A) Back support

(c) Thermal oxidation and metal deposition of CPW.

(d) Patterning of Cytosol polymer.

(e) Patterning of photoresist and oxide.

(f) DRIE etching of through-holes and stripping of photoresist.

(B) Base substrate

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(D) Wafer bonding, DRIE etching and metallization

Figure 6.8. Fabrication sequence of the RF MEMS shunt switch: (A) bottom back support layer, (B) middle perforated base substrate layer, (C) top diaphragm layer and (D) wafer bonding, DRIE etching and metallization.

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Figure 6.9. A SEM picture of the fabricated base substrate with perforations.

Figure 6.10. A SEM picture (top view) of the fabricated RF MEMS shunt switch.

6.5 Device Characterization

Figure 6.11 illustrates the measurement setup for the characterization of the switching times of the fabricated RF MEMS shunt switch. The switching time is obtained by measuring the time delay between the actuating voltage change and the corresponding output signal change. As such, two switching times are measured for the RF MEMS switch: (1) pull-in time and (2) release time. The pull-in time is defined as the time needed by the contact metal, which is underneath the diaphragm, to be pulled downwards by the generated electrostatic force to make physical contact with the base substrate. The release time is defined as the time needed by the diaphragm to spring back to its initial unactuated position upon the removal of the actuating voltage. A square waveform with a 20% duty cycle (a unipolar voltage amplitude of 30 V with a DC bias voltage of 30 V) is applied as the actuating voltage to the RF MEMS switch and monitored by a digital oscilloscope. This translates to an actuating voltage of 60 V at the “ON” state and 0 V at the “OFF” state of the square waveform.

Figure 6.12 illustrates the theoretical and experimental switching times of the RF MEMS shunt switch with and without perforation scheme in the base substrate. As illustrated, with perforations in the base substrate, the pull-in time is reduced significantly from 5.40 ms to 0.44 ms. This represents a switching time reduction of more than 12 times when compared to the base substrate without any perforations. Similarly, after the implementation of perforations in the base substrate, the release time is also significantly decreased from 40.60 ms to 3.20 ms, which represents a decrease of nearly 13 times. The theoretical results are also in pretty good agreements with those measured ones.

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Figure 6.11. A measurement setup for the characterization of the switching times of the fabricated RF MEMS shunt switch.

Figure 6.12. Theoretical and experimental switching times of the RF MEMS shunt switch with and without perforations in the base substrate.

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Although the switching times are reduced significantly by the introduction of perforations in the base substrate, the pull-in and release times of the RF MEMS shunt switch are in the millisecond range. As such, it must be pointed out that the switching speed of the current switch design is still quite slow despite the implementation of the perforated base substrate. The high switching times can be attributed to the utilization of the diaphragm structure as the electrostatic actuation scheme since it is less compliance than the bridge or cantilever structures. Usually, the fast switching type of RF switches employ electrostatic actuated bridge or cantilever structures with switching times in the microsecond regime [1, 2, 4, 7]. However, despite the switching speed limitation, the fabricated RF MEMS switch can still be deployed in sub-second switching applications such as the automated reconfiguration of telecommunication networks [26].

The fabricated RF MEMS shunt switch is also characterized for its RF performance using HP 8722D network analyzer and Cascade GSG microwave probes. Figure 6.13 illustrates the theoretical and measurement results of the isolation and insertion loss of the RF MEMS switch. As illustrated, the measured isolation is below -40 dB up to 14 GHz and about -35 dB at 25 GHz with an air gap of 8 μm . The measured insertion loss is about -0.7 dB up to 4 GHz and -2.1 dB at 25 GHz for the RF MEMS switch with a 5 mm long CPW transmission line. The theoretical results are also in good agreements with those measured ones.

Figure 6.13. Theoretical and measurement results of the isolation and insertion loss of the RF MEMS shunt switch.

6.6 Conclusion

A wafer-level encapsulated RF MEMS shunt switch, which is of a 3-layer stacked wafer design, is presented. The design consists of a top corrugated low-stress diaphragm, a middle perforated base substrate and a bottom back support incorporating an air cavity. To enhance the switching speed of the RF MEMS switch, the air gap is optimized and through-holes are strategically implemented in the base substrate. The increase in the switching speed can be attributed to the significant reduction of the squeeze-film damping effect. The pull-in and release times of the RF MEMS switch

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are significantly reduced from 5.40 ms to 0.44 ms and 40.60 ms to 3.20 ms, respectively, when comparisons are made with one without any perforations, which represents a switching time reduction of more than 12 times. The measured isolation is below -40 dB up to 14 GHz and about -35 dB at 25 GHz with an air gap of 8 μm . The measured insertion loss is about -0.7 dB up to 4 GHz and -2.1 dB at 25 GHz for the RF MEMS switch with a 5 mm long CPW transmission line.

References

- [1] D. Hyman *et al.*, "Surface-micromachined RF MEMS switches on GaAs substrates," *Int. J. RF Microwave CAE*, vol. 9, pp. 348–361 (1999).
- [2] P. M. Zavracky, N. E. McGruer, R. H. Morrison and D. Potter, "Microswitches and microrelays with a view toward microwave applications," *Int. J. RF Microwave CAE*, vol. 9, pp. 338–347 (1999).
- [3] R. E. Mihailovich *et al.*, "MEMS relay for reconfigurable RF circuits," *IEEE Microwave Wireless Components Lett.*, vol. 11, pp. 53–55 (2001).
- [4] S. Duffy, C. Bozler, S. Rabe, J. Knecht, L. Travis, P. Wyatt, C. Keast and M. Gouker, "MEMS microswitches for reconfigurable microwave circuitry," *IEEE Microwave Wireless Components Lett.*, vol. 11, pp. 106–108 (2001).
- [5] B. Schauwecker, K. M. Strohm, W. Simon, J. Mehner and J. F. Luy, "A new type of high bandwidth RF MEMS switch – Toggle switch," *J. Semicond. Technol. Sci.*, vol. 2, pp. 237–245 (2002).
- [6] J. Oberhammer and G. Stemme, "Design and fabrication aspects of an S-shaped film actuator based DC to RF MEMS switch," *J. Microelectromech. Syst.*, vol. 13, pp. 421–428 (2004).
- [7] C. L. Goldsmith, Z. Yao, S. Eshelman and D. Denniston, "Performance of low-loss RF MEMS capacitive switches," *IEEE Microwave Guided Wave Lett.*, vol. 8, pp. 269–271 (1998).
- [8] C. Chang and P. Chang, "Innovative micromachined microwave switch with very low insertion loss," *Sens. Actuators A*, vol. 79, pp. 71–75 (2000).
- [9] J. B. Muldavin and G. M. Rebeiz, "Inline capacitive and DC-contact MEMS shunt switches," *IEEE Microwave Wireless Components Lett.*, vol. 11, pp. 334–336 (2001).
- [10] J. Tsaur *et al.*, "Broadband MEMS shunt switches using PZT/HfO₂ multilayered high *k* dielectrics for high switching isolation," *Sens. Actuators A*, vol. 121, pp. 275–281 (2005).
- [11] Y. T. Song, H. Y. Lee and M. Esashi, "A corrugated bridge of low residual stress for RF-MEMS switch," *Sens. Actuators A*, vol. 135, pp. 818–826 (2007).
- [12] G. M. Rebeiz, *RF MEMS Theory, Design, and Technology*, John Wiley & Sons, 2003.
- [13] G. L. Tan and G. M. Rebeiz, "A DC-contact MEMS shunt switch," *IEEE Microwave and Wireless Component Lett.*, vol. 12, pp. 212–214 (2002).

- [14] S. C. Shen, D. Caruth and M. Feng, "Broadband low actuation voltage RF MEMS switches," *Proc. 22nd IEEE GaAs IC Symp.*, Seattle, WA, pp. 161–164, 5–8 November 2000.
- [15] F. Ke, J. Miao and C. W. Tan, "Performance enhancement by substrate perforation for a wafer-level encapsulated RF MEMS DC shunt switch," *Proc. 22nd IEEE International Conference on Micro Electro Mechanical Systems*, Sorrento, Italy, pp. 876–879, 25–29 January 2009.
- [16] X. Li, R. Lin, H. S. Kek, J. Miao and Q. Zou, "Sensitivity-improved silicon condenser microphone with a novel single deeply corrugated diaphragm," *Sens. Actuators A*, vol. 2984, pp. 1–6 (2001).
- [17] J. Miao, R. Lin, L. Chen, Q. Zou, S. Y. Lim and S. H. Seah, "Design considerations in micromachined silicon microphones," *Microelectron. J.*, vol. 33, pp. 21–28 (2002).
- [18] M. Földner, A. Dehé and R. Lerch, "Analytical analysis and finite element simulation of advanced membranes for silicon microphones," *IEEE Sens. J.*, vol. 5, pp. 857–863 (2005).
- [19] F. Ke, J. Miao and J. Oberhammer, "A Ruthenium-based multimetal-contact RF MEMS switch with a corrugated diaphragm," *J. Microelectromech. Syst.*, vol. 17, pp. 1447–1459 (2008).
- [20] F. Ke, J. Miao and Z. Wang, "A wafer-scale encapsulated RF MEMS switch with a stress-reduced corrugated diaphragm," *Sens. Actuators A*, pp. 151, pp. 237–243 (2009).
- [21] J. B. Starr, "Squeeze-film damping in solid-state accelerometers," *IEEE Workshop in Solid-State Sensor and Actuator 4th Technical Digest*, pp. 44–47 (1990).
- [22] T. B. Gabrielson, "Mechanical-thermal noise in micromachined acoustic and vibration sensors," *IEEE Trans. Electron Devices*, vol. 40, pp. 903–909 (1993).
- [23] A. J. Zuckerwar, "Theoretical response of condenser microphones," *J. Acoust. Soc. Am.*, vol. 64, pp. 1278–1285 (1978).
- [24] A. J. Zuckerwar, "Principles of operation of condenser microphones," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.
- [25] P. G. Steeneken *et al.*, "Dynamics and squeeze film gas damping of a capacitive RF MEMS switch," *J. Micromech. Microeng.*, vol. 15, pp. 176–184 (2005).

- [26] S. Braun, J. Oberhammer and G. Stemme, "Single-chip MEMS 5 x 5 and 20 x 20 double-pole single-throw switch arrays for automating telecommunication networks," *J. Micromech. Microeng.*, vol. 18, pp. 1–11 (2008).

Chapter 7

Conclusion and Future Work

7.1 Summary of Accomplished Work

An overview of MEMS devices and microsystems has been briefly introduced, which highlights the viscous damping effect with respect to the mechanical performance of MEMS devices. The viscous damping effect becomes more and more pronounced as the microstructure gets smaller and smaller. A literature review on squeeze-film damping, which covers the aspects of frequency response characteristics, border effect of plate edges, viscous damping of perforated plate, effects of compressibility, inertia and rarefied gas, and mechanical-thermal noise, has been concisely presented. A critique of the existing literature has also been summarized and solutions have been proposed to overcome these limitations. Depending on the types of applications, the introduction or the deficiency of viscous damping in mechanical structures can be made use of to enhance the mechanical performance of miniaturized devices desirably.

The condenser microphone theory has been briefly reviewed. With reference to a B&K MEMS technology microphone, the theoretical results obtained by a MATLAB program are in very good agreements with those reported experimental ones. The simple Škvor/Starr approach in the computation of the mechanical resistance does not consider the location effect of holes in the backplate. A modified Škvor/Starr approach has been proposed, in which a Bessel function-based mode shape factor is incorporated into the mechanical resistance expression of Škvor to account for the effect of the hole

location. With reference to two B&K microphones, the theoretical results obtained by the modified approach are in good agreements with those reported experimental ones.

A 2^6 factorial design with a single replicate has been carried out on a comprehensive condenser microphone model on the basis of six backplate design parameters and four output responses. The main effects of air gap and slot have been found to have significant influences over the sensitivity, mechanical-thermal noise and bandwidth while the slot/hole location interaction has significant effects on the latter two responses. The remaining main effects of hole number, hole size and backplate thickness have been found to have only minor influences over the bandwidth.

By attaching a silicon backplate with optimized air gap, slots and holes to a PZT/Si diaphragm, it has been demonstrated experimentally that its frequency response characteristics can be optimized desirably. The attached backplate increases the viscous damping effect by introducing damping primarily in the air gap, slots and holes. The bandwidth of the PZT/Si acoustic transducer is approximately 18 kHz with an estimated *A*-weighted mechanical-thermal noise of 28.6 dB(A).

A wafer-level encapsulated RF MEMS shunt switch, which is of a 3-layer stacked wafer design, has been presented. To enhance its switching speed, the air gap is optimized while through-holes are strategically implemented in the base substrate. The increase in the switching speed can be attributed to the significant reduction of the squeeze-film damping effect. The pull-in and release times of the RF MEMS switch are significantly reduced from 5.40 ms to 0.44 ms and 40.60 ms to 3.20 ms, respectively, when comparisons are made with one without any perforation scheme. This represents a switching time reduction of more than 12 times.

7.2 Major Contributions

The major contributions of the accomplished work are listed below:

- Zuckerwar's comprehensive condenser microphone theory, which was previously developed for the traditional condenser microphone, has been successfully adapted for the MEMS technology condenser microphone with a very good degree of accuracy.
- An important conclusion has been reached that the location of holes in the backplate has a significant effect on the frequency response characteristics and the mechanical resistance (or mechanical-thermal noise), which was not considered or highlighted in previous literatures for diaphragm-based electrostatic transducers.
- For the very first time, a 2^6 factorial design with a single replicate has been carried out on a comprehensive condenser microphone model developed by Zuckerwar on the basis of six backplate parameters and four output responses. By ranking these six parameters in relative significance, the optimization of viscous damping via the backplate design is made more systematic and simpler.
- A Bessel function-based mode shape factor has been incorporated into the mechanical resistance expression of Škvor to account for the hole location effect on the viscous damping effect and to minimize its overestimation.
- Using a PZT/Si acoustic transducer and a RF MEMS shunt switch as examples, it has been successfully demonstrated that the introduction or the deficiency of viscous damping in diaphragm-based mechanical structures can be utilized to enhance the mechanical performance of these devices.

7.3 Future Work

For two parallel plates, the equivalent mechanical resistance is inversely proportional to the air gap to the power of three. Thus, for an air gap of several microns, the squeeze-film damping effect dominates the dissipation mechanism of such structure. As such, the mechanical performance of diaphragm-based MEMS devices is, to a great extent, dependent on such viscous damping effect (i.e. air gap). For future work, some suggestions, which are mostly associated with squeeze-film damping, are briefly summarized with respect to modeling, device performance, fabrication and characterization.

7.3.1 Modeling

In Chapter 4, it has been pointed out that the application of the polarization voltage induces a static deflection of the diaphragm, which, when combined with an incident pressure, alters the deflection mode. For large air gap electrostatic transducers, the impact on the squeeze-film damping effect is usually small and can be neglected. However, on the other hand, for small air gap electrostatic transducers, this can have a significant influence on the damping characteristics and must be taken into consideration. As such, it is recommended that a polarization voltage correction factor be incorporated into the modified Škvor/Starr approach to account for the increase in the mechanical resistance due to a small polarized air gap.

7.3.2 Device Performance

For the PZT/Si acoustic transducer and the RF MEMS shunt switch, it has been

successfully demonstrated at the device level (i.e. local damping effect) that their damping characteristics can be enhanced very specifically by attaching an optimized Si backplate and base substrate, respectively, in close proximity to the diaphragms. Subsequently, the next step is to investigate the next higher level of packaging effect (i.e. global damping effect) on the damping characteristics of both devices. Hence, it is recommended that a detailed theoretical and experimental analysis be carried out to examine the damping effect of packaging on the performance of both devices.

For the RF MEMS shunt switch, via the implementation of through-holes in the base substrate, its switching speed has been significantly enhanced by a factor of more than 12 through the reduction of the squeeze-film damping effect. Subsequently, to enhance the switching speed further, it is recommended that a slot be incorporated around the circumference of the base substrate. Due to the presence of the CPW in the base substrate and fabrication constraint, the slot has to be split up into several shorter ones. However, extreme care must be undertaken not to implement a slot that is too wide such that the electrostatic force to actuate the diaphragm reduces significantly as a result of a reduction in the electrode area in the base substrate.

7.3.3 Fabrication

For the PZT/Si acoustic transducer, the air gap is created by etching a cavity in the Si backplate wafer. Since the air gap has a significant influence on the squeeze-film damping effect, it is imperative that the air gap must be accurately and reliably etched to the desired design specification in addition to achieving a good etching uniformity throughout the wafer. As such, it is recommended that a SOI wafer be employed to

fabricate the Si backplate since the thickness of the device layer of the SOI wafer has a very good manufacturing tolerance control while the embedded SiO₂ etch-stop layer ensures that an uniform etching of the air gap cavity in the device layer can be achieved throughout the SOI wafer. Similarly, for the RF MEMS shunt switch, a SOI wafer can also be used for the diaphragm wafer so that during the DRIE etching of a shallow recess, the air gap can be accurately and reliably etched to the desired depth.

For the RF MEMS shunt switch, the average process yield is more than 70%. The most prominent fabrication issue is the breakage of the diaphragms at the corrugated structure. The diaphragms are observed to be peeling off at the corrugation edges where the maximum stress concentrations are situated. During the DRIE etching process to release the corrugated diaphragms (also function as an etch-stop layer), it is observed that the etching process is non-uniform throughout the diaphragm wafer where the etch rate is slower at the center than the edge. As such, in order to completely release all the diaphragms, the already released diaphragms at the wafer center have to be subjected to additional unnecessary etching, which would then compromise their structural integrity at the corrugations. Therefore, it is recommended that the process parameters of the DRIE etching machine be optimized so that a uniform etching can be achieved throughout the wafer to increase the process yield.

During the fabrication of the RF MEMS shunt switch, the diaphragm, base substrate and back support wafers are aligned and then thermally bonded together using the Cytop polymer as the intermediate bonding layer. Although the Cytop polymer has its own merits, such as low bonding temperature (160 °C), bondable to most materials and good thickness control due to spin-coating, as a bonding polymer, it

does suffer from a drawback of out-gassing, which would then affect the reliability of the contact metals, during long term applications. As such, it is recommended that alternative wafer bonding techniques and bonding materials be considered for the assembly of the diaphragm, base substrate and back support wafers. For example, the thermal compression or eutectic wafer bonding techniques can be used to provide the hermetic packaging of the devices instead of polymer bonding. Other hermetic or quasi-hermetic sealing materials, such as liquid crystal polymer (LCP), can be used to replace the Cytop polymer as the bonding material.

7.3.4 Characterization

For low frequency MEMS applications, the viscous damping effect (mechanical resistance) dominates the dissipation mechanism over the elastic damping effect (mechanical compliance). As such, the mechanical resistance plays a significant role in the performance of sensors (mechanical-thermal noise) and actuators (actuating/switching speed). Although this resistance cannot be measured directly, it can be indirectly inferred from the performance characteristics of the devices.

For the RF MEMS shunt switch, both the pull-in and release times are proportional to the mechanical resistance (Eqs. (6.1), (6.4), (6.7) and (6.12)). Thus, by measuring both switching times, the mechanical resistance can be calculated and verified against the theoretical value. For the PZT/Si acoustic transducer, the mechanical resistance can be deduced from its measured noise spectrum (or noise voltage spectral density plot) in the microphone mode. The preamplifier noise dominates the spectrum below 500 Hz while the mechanical-thermal noise dominates

from 500 Hz to 20 kHz. Hence, for the calculation of the mechanical resistance, only the noise spectrum from 500 Hz to the first resonant frequency is required. From the spectrum, the noise pressure spectral density ($\text{Pa}/\sqrt{\text{Hz}}$) is obtained from the noise voltage spectral density ($\text{V}/\sqrt{\text{Hz}}$) by factoring in the microphone sensitivity (V/Pa) and the preamplifier gain (Eq. (A.25)). After that, using the obtained equivalent noise pressure, the mechanical resistance can be approximated using Eq. (A.24).

While the switching times of the RF MEMS switch have been characterized, the noise spectrum of the PZT/Si acoustic transducer in the microphone mode has not been measured. Therefore, for any future work involving the acoustic transducer, it is recommended that the mechanical resistance be determined as well via the measurement of the noise spectrum.

7.3.5 Other Suggestion

The resonant frequency of a mechanical structure is governed by its geometry and material properties (i.e. mass and stiffness at supports) while damping is predominantly determined by the structural configurations (i.e. cantilever, bridge, diaphragm or comb-drive) and the surrounding media. For a cantilever, in most applications, the resonating mode is desired where there is no or minimum damping. For a bridge or diaphragm suspended above a substrate, it is strongly influenced by the squeeze-film damping effect. There are two types of comb-drives: (1) laterally and (2) vertically driven ones, which are dominated by slide film and squeeze-film damping, respectively. For high Q resonating applications, the laterally driven type is desired as it has a larger oscillation and lower damping as compared to the vertically driven one,

in which losses are larger for the vertical displacement of fluid due to the very small air gap between the comb-fingers. As such, detailed studies on the vertically driven comb-drive are rare in the literature. However, it also represents an excellent opportunity to carry out a detailed study on the dynamic behavior of such a structure for future work.

Appendix A

Condenser Microphone Theory

The condenser microphone theory is briefly reviewed, which follows the analysis of Zuckerwar [1, 2]. The theoretical considerations are based on a circular diaphragm of radius a . Figure A.1(a) illustrates the nomenclature of the condenser microphone while Fig. A.1(b) illustrates the arrangement of acoustic slot and holes in the backplate.

A.1 Pull-in Voltage

Assuming that the diaphragm tension dominates over its inertia, the critical pull-in voltage of the diaphragm, for a piston model and a rigid backplate, can be approximated by

$$V_m = \left(\frac{64Th^3}{27\epsilon_o a^2} \right)^{1/2} \quad (\text{A.1})$$

where T is the diaphragm tension, h is the unpolarized air gap, and ϵ_o is the dielectric permittivity of free air (8.85×10^{-12} F/m). If the diaphragm is very thin and has a high residual stress, then the pull-in voltage needs to be multiplied by a factor of 0.82, which is the optimum ratio between the diaphragm and the backplate area for maximum sensitivity [3, 4]. A high pull-in voltage results from a high diaphragm tension, large air gap, and small diaphragm.

Fig. A.1. Condenser microphone: (a) Cross-sectional view and (b) arrangement of acoustic slot and holes in the backplate.

A.2 Electrical Sensitivity

The electrical sensitivity of the microphone, with an acoustic slot around the circumference of the backplate, can be approximated by

$$M_e = \frac{E_o}{h_o} \left(1 - \frac{1}{2} \cdot \frac{S_{bp}}{S_{dia}} \right) \quad (\text{A.2})$$

where h_o is the average air gap distance between the diaphragm and the backplate as a result of the electrostatic deflection caused by the polarization voltage E_o , S_{dia} is the diaphragm area, and S_{bp} is the backplate area. For a circular diaphragm and backplate, the ratio of S_{bp}/S_{dia} is simply $(b/a)^2$, where b is the backplate radius. This correction factor of $(1 - b^2/2a^2)$ significantly enhances the electrical description of the circular microphone [5]. A high electrical sensitivity is favored by a high polarization voltage, small air gap, and large diaphragm to backplate area ratio.

A.3 Mechanical Sensitivity

The vibration motion of the system of coupled diaphragm and air layer is governed by the following four equations [1, 2]:

- (1) Equation of motion

$$\nabla^2 y(r, \theta) + K^2 y(r, \theta) = -\frac{p_i}{T} + \frac{p(r, \theta, 0)}{T} \quad (\text{A.3})$$

- (2) Navier-Stokes equation

$$j\omega\rho_0\mathbf{v} = -\nabla p + \mu(\nabla^2\mathbf{v} + \frac{1}{3}\nabla\nabla\cdot\mathbf{v}) \quad (\text{A.4})$$

- (3) Continuity equation

$$\rho_0\nabla\cdot\mathbf{v} = -j\omega\rho \quad (\text{A.5})$$

(4) Equation of state

$$p = \rho c_T^2 \quad (\text{A.6})$$

With reference to Fig. A.1 and a cylindrical coordinate system (r, θ, Z) , $y(r, \theta)$ is the deflection of the diaphragm, p_i is the incident acoustic pressure, $p(r, \theta, 0)$ is the reaction pressure on the diaphragm, ω is the angular frequency of the incident acoustic pressure, v is the air particle velocity, ρ_o is the static air density (1.205 kg/m^3), ρ is the time-varying air density, μ is the absolute viscosity of air ($17.9 \times 10^{-6} \text{ Ns/m}^2$), c_T is the isothermal speed of sound in air (290.2 m/s), and K is the wave number of sound in the diaphragm,

$$K = \omega \left(\frac{\sigma_M}{T} \right)^{1/2} \quad (\text{A.7})$$

where σ_M is the mass surface density of the diaphragm.

Each acoustic hole in the backplate is assigned a number ($k = 1, 2, \dots, q$) for identification where q is the total number of acoustic holes. If an acoustic slot is present around the circumference of the backplate, it is assigned as $k = 1$. An equivalent circuit of the backplate and the backchamber is illustrated in Fig. A.2. The equations of the impedance circuit can be written in a matrix form

$$\mathbf{P} = \mathbf{Z}\mathbf{U} \quad (\text{A.8})$$

where $\mathbf{P}$ is the $(q \times 1)$ pressure matrix, $\mathbf{U}$ is the $(q \times 1)$ volume velocity matrix and $\mathbf{Z}$ is the $(q \times q)$ acoustic impedance matrix,

$$\mathbf{P} = \begin{bmatrix} p_1 \\ p_2 \\ \vdots \\ \vdots \\ p_q \end{bmatrix}$$

$$\mathbf{U} = \begin{bmatrix} U_1 \\ U_2 \\ \vdots \\ \vdots \\ U_q \end{bmatrix}$$

$$\mathbf{Z} = \begin{bmatrix} Z_1 + Z_C & Z_C & \cdot & \cdot & Z_C & Z_C \\ Z_C & Z_2 + Z_C & & & \cdot & \cdot \\ \cdot & Z_C & & & \cdot & \cdot \\ \cdot & \cdot & & & \cdot & \cdot \\ \cdot & \cdot & & & Z_{q-1} + Z_C & Z_C \\ Z_C & \cdot & \cdot & \cdot & Z_C & Z_q + Z_C \end{bmatrix} \quad (\text{A.9})$$

Fig. A.2. Equivalent circuit of the acoustic slot and holes in the backplate.

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The acoustic impedance of the backchamber, Z_C , can be modeled as

$$Z_C = \frac{\gamma \rho_o c_T^2}{j \omega V} \quad (\text{A.10})$$

where γ is the specific heat ratio of air (1.403) and V is the volume of the backchamber.

The acoustic impedance of the k th opening of the backplate can be modeled as

$$Z_k = \frac{(8 \omega \rho_o \mu)^{1/2}}{\pi r_k^2} \left(1 + \frac{l_k}{2 r_k} \right) + j \frac{\omega \rho_o (l_k + 1.7 r_k)}{\pi r_k^2} \quad (\text{A.11})$$

where r_k is the radius of the k th hole.

The acoustic impedance of the slot in the backplate can be modeled as

$$Z_1 = \frac{(2 \omega \rho_o \mu)^{1/2}}{\pi a_1 r_1} \left(1 + \frac{l_1}{2 r_1} \right) + j \frac{\omega \rho_o l_1}{2 a_1 r_1} \quad (\text{A.12})$$

where r_1 is the width of the slot.

Without going into the details of further mathematical derivations, of which can be found in Refs. [1, 2], the deflection of the diaphragm, $y(r)$, independent of θ , can be written down as

$$y(r) = \frac{p_i}{TK^2} \cdot \frac{J_2(Ka)}{J_0(Ka) + D(\omega)} \quad (\text{A.13})$$

where $J_0(Ka)$ and $J_2(Ka)$ are the zero and second order Bessel functions of Ka , respectively and $D(\omega)$, which is the dimensionless air layer damping term, is given by

$$D(\omega) = \frac{j 4 \omega \rho_o J_o(Ka)}{\pi T J_2(Ka)} \sum_{m=0}^{\infty} \left[-j \omega \left(\frac{y_m}{y_o} \right) + \sum_{s=1}^q \left(\frac{f_s}{y_o} \right) s_s \Gamma_m J_o(\xi_m a_s) \right] \quad (\text{A.14})$$

$$\times \frac{1}{T_m J_0(\xi_m a)} \left\{ \frac{J_2(Ka) \delta(m)}{4 K^2 a^2} + \frac{J_1(Ka) [1 - \delta(m)]}{2 Ka (K^2 a^2 - \xi_m^2 a^2)} \right\}$$

Appendix A Condenser Microphone Theory

where m is the number of summation terms, y_m is the diaphragm displacement function (Eq. (3.32) of Ref. [2]), y_o is the interim unknown diaphragm displacement in a trial expression (Eq. (3.44) of Ref. [2]), f_s is the air velocity at the k th hole in the backplate, S_s is the area of the k th opening in the backplate, Γ_m and T_m are numerical factors (Eqs. (3.34) and (3.35) of Ref. [2], respectively), ξ_m is the symmetric radial component of scalar wave number, a_s is the location of the k th opening in the backplate, $\delta(m)$ is a conditional function ($\delta(m) = 1$ if $m = 0$ and $\delta(m) = 0$ otherwise), and $J_1(Ka)$ is the first order Bessel function of Ka . From Eq. (A.13), the mechanical sensitivity of the microphone is given by

$$M_m(\omega) = \frac{y(r)}{p_i} = \frac{1}{TK^2} \cdot \frac{J_2(Ka)}{J_0(Ka) + D(\omega)} \quad (\text{A.15})$$

At low frequencies, the mechanical sensitivity of Eq. (A.15) can be approximated by

$$M_m \approx \frac{a^2}{8T} \quad (\text{A.16})$$

Figure A.3 illustrates the acoustical lumped-element equivalent circuit of the microphone. At frequencies that are well below the first resonant frequency of the diaphragm, the diaphragm mass, diaphragm compliance, air compliance, and air resistance can be expressed respectively as

$$M_M = \frac{4\sigma_M}{3\pi a^2} \quad (\text{A.17})$$

$$C_M = \frac{(\pi a^2)^2}{8\pi T} \quad (\text{A.18})$$

$$C_A(\omega) = \frac{(\pi a^2)^2}{8\pi T \operatorname{Re}(D)} \quad (\text{A.19})$$

$$R_A(\omega) = \frac{8\pi T \operatorname{Im}(D)}{\omega(\pi a^2)^2} \quad (\text{A.20})$$

where $\operatorname{Re}(D)$ and $\operatorname{Im}(D)$ are the real and imaginary parts of $D(\omega)$, respectively. The air resistance R_A , whose physical origin lies in the viscous boundary layer damping losses in the air gap, slot and holes in the backplate, accounts for the mechanical-thermal noise in the microphone.

Fig. A.3. Acoustical lumped-element equivalent circuit of the microphone.

A.4 Microphone Sensitivity

The open-circuit sensitivity of the microphone at low frequency can be expressed by

$$M_{oc} = M_e M_m = \frac{E_o}{h_o} \left(1 - \frac{b^2}{2a^2} \right) \cdot \frac{a^2}{8T} \quad (\text{A.21})$$

The overall sensitivity of the microphone, including the preamplifier, is given by

$$M = M_{oc} A_c A_v = \frac{E_o}{h_o} \left(1 - \frac{b^2}{2a^2} \right) \frac{a^2}{8T} \left(1 + \frac{C_i + C_s}{C_{to}} \right)^{-1} \quad (\text{A.22})$$

where C_{to} is the capacitance of the polarized microphone, C_i is the input capacitance of a preamplifier, C_s is the stray capacitance, A_c is the capacitive signal attenuation due to C_i and C_s , and A_v is the gain of the preamplifier, which is assumed to be one.

A.5 Mechanical-thermal Noise

The background noise level of a microphone is one of the most important design specifications because it imposes a restriction on the microphone sensitivity. The background noise consists of both external sources, such as power supply lines, radio frequency interference, and vibration, and internal generated ones such as mechanical-thermal noise [6, 7], electrical Johnson noise [7–9], shot noise [7–9], $1/f$ noise [7–10], and preamplifier noise [7, 8]. In the formulation [8] of the preamplifier noise, the effects of electrical Johnson noise, shot noise, and $1/f$ noise are clearly visible.

The movement of small movable mechanical components is adversely affected by molecular thermal agitation, which subsequently gives rise to the mechanical-thermal noise. This mechanical-thermal noise can put a limiting factor on the performance of many small-sensor systems. The mechanical-thermal noise, together with other sources of background noise, establishes the lowest limit of acoustic pressure [8, 11] that can be picked up by the microphone. One method of reducing the mechanical-thermal noise is to reduce the air resistance through the meticulous design of the air gap, slot and holes in the backplate.

The equivalent mechanical resistance due to the air gap, slot and holes, which is frequency dependent, can be re-expressed as

$$R_A(f) = \frac{8\pi T \operatorname{Im}(D)}{2\pi f (\pi a^2)^2} \quad (\text{A.23})$$

where f is the frequency of the incident pressure. The pressure spectral density of the mechanical-thermal noise can then be expressed by

$$P_f = \sqrt{4k_B T R_A(f)} \quad (\text{A.24})$$

where k_B is the Boltzmann constant (1.38×10^{-23} J/K) and T is the absolute temperature (K). The noise voltage spectral density of the mechanical-thermal noise can be expressed by

$$N_f = M P_f \quad (\text{A.25})$$

Finally, the background noise pressure level (A -weighted) of the mechanical-thermal noise [12] can be expressed by

$$N_T = \sqrt{\int_{f_1}^{f_2} 4k_B T R_A(f) A^2(f) df} \quad (\text{A.26})$$

where $A(f)$ is the function of the A -weighted filter, and f_1 and f_2 are 10 Hz and 20 kHz, respectively.

A.6 C_A/C_M Ratio

The C_A/C_M ratio [13] can be expressed by

$$\frac{C_A}{C_M} = \frac{1}{\operatorname{Re}(D)} \quad (\text{A.27})$$

This ratio determines whether the diaphragm or the underlying air layer controls the microphone sensitivity. The diaphragm is the better choice as the air layer is much

more temperature sensitive and less stable. For this to be realized, C_A should be larger than C_M .

References

- [1] A. J. Zuckerwar, "Theoretical response of condenser microphones," *J. Acoust. Soc. Am.*, vol. 64, pp. 1278–1285 (1978).
- [2] A. J. Zuckerwar, "Principles of operation of condenser microphones," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.
- [3] P. R. Scheeper, A. G. H. van der Donk, W. Olthuis and P. Bergveld, "A review of silicon microphones," *Sens. Actuators A*, vol. 44, pp. 1–11 (1994).
- [4] J. E. Warren, "Capacitance microphone static membrane deflections: Comments and further results," *J. Acoust. Soc. Am.*, vol. 58, pp. 733–740 (1975).
- [5] M. S. Hawley, F. F. Romanow and J. E. Warren, "The Western Electric 640AA capacitance microphone: Its history and theory of operation," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 2, pp. 8–34.
- [6] T. B. Gabrielson, "Mechanical-thermal noise in micromachined acoustic and vibration sensors," *IEEE Trans. Electron Devices*, vol. 40, pp. 903–909 (1993).
- [7] T. B. Gabrielson, "Fundamental noise limits in miniature acoustic and vibration sensors," *J. Vib. Acoust.*, vol. 117, pp. 405–410 (1995).
- [8] V. Tarnow, "Thermal noise in microphones and preamplifiers," *B&K Technical Review*, vol. 3, pp. 3–14 (1972).
- [9] A. J. Zuckerwar, T. R. Kuhn and R. M. Serbyn, "Background noise in piezoresistive, electret condenser and ceramic microphones," *J. Acoust. Soc. Am.*, vol. 113, pp. 3179–3187 (2003).
- [10] A. J. Zuckerwar and K. C. T. Ngo, "Measured $1/f$ noise in the membrane motion of condenser microphones," *J. Acoust. Soc. Am.*, vol. 95, pp. 1419–1425 (1994).

Appendix A Condenser Microphone Theory

- [11] V. Tarnow, "The lower limit of detectable sound pressure," *J. Acoust. Soc. Am.*, vol. 82, pp. 379–381 (1987).
- [12] C. W. Tan and J. M. Miao, "Analytical modeling for bulk-micromachined condenser microphones," *J. Acoust. Soc. Am.*, vol. 120, pp. 750–761 (2006).
- [13] C. W. Tan and J. M. Miao, "Design optimization of condenser microphone: A design of experiment perspective," *J. Acoust. Soc. Am.*, vol. 125, pp. 3641–3649 (2009).

Appendix B Factorial Design Data

Data of the single-replicate ($N=1$) 2^6 factorial design.

Run Number	Factor A	Factor B	Factor C	Factor D	Factor E	Factor F	Run Label	Open-circuit sensitivity (dB ref. 1 V/Pa)	Bandwidth (kHz)	Mechanical-thermal noise [dB(A)]	C_A/C_M ratio
1	-	-	-	-	-	-	(1)	-33.2	15.0	23.0	16.1
2	+	-	-	-	-	-	a	-35.6	20.0	19.8	16.1
3	-	+	-	-	-	-	b	-36.5	2.5	30.6	16.1
4	+	+	-	-	-	-	ab	-39.0	5.0	27.4	16.3
5	-	-	+	-	-	-	c	-33.2	7.5	25.7	16.2
6	+	-	+	-	-	-	ac	-35.6	12.0	22.6	16.2
7	-	+	+	-	-	-	bc	-36.5	6.5	26.1	16.3
8	+	+	+	-	-	-	abc	-39.0	8.25	23.3	16.4
9	-	-	-	+	-	-	d	-33.2	17	22.9	16.1
10	+	-	-	+	-	-	ad	-35.6	25	19.6	16.1
11	-	+	-	+	-	-	bd	-36.5	2.5	30.5	16.0
12	+	+	-	+	-	-	abd	-39.0	5.5	27.3	16.2
13	-	-	+	+	-	-	cd	-33.2	8.5	25.5	16.2
14	+	-	+	+	-	-	acd	-35.6	14	22.3	16.2
15	-	+	+	+	-	-	bcd	-36.5	8	25.8	16.2
16	+	+	+	+	-	-	abcd	-39.0	11	22.8	16.2
17	-	-	-	-	+	-	e	-33.2	19	22.8	16.1
18	+	-	-	-	+	-	ae	-35.6	28	19.4	16.1
19	-	+	-	-	+	-	be	-36.5	2.5	30.4	15.9
20	+	+	-	-	+	-	abe	-39.0	6	27.1	16.2

Appendix B Factorial Design Data

Run Number	Factor A	Factor B	Factor C	Factor D	Factor E	Factor F	Run Label	Open-circuit sensitivity (dB ref. 1 V/Pa)	Bandwidth (kHz)	Mechanical-thermal noise [dB(A)]	C_A/C_M ratio
21	-	-	+	-	+	-	ce	-33.2	10	25.4	16.1
22	+	-	+	-	+	-	ace	-35.6	17	22.1	16.1
23	-	+	+	-	+	-	bce	-36.5	10	25.6	16.2
24	+	+	+	-	+	-	abce	-39.0	14.5	22.4	16.2
25	-	-	-	+	+	-	de	-33.2	20	22.8	16.1
26	+	-	-	+	+	-	ade	-35.6	32.5	19.4	16.1
27	-	+	-	+	+	-	bde	-36.5	2.75	30.4	15.9
28	+	+	-	+	+	-	abde	-39.0	6	27.1	16.1
29	-	-	+	+	+	-	cde	-33.2	10	25.4	16.1
30	+	-	+	+	+	-	acde	-35.6	19	22.0	16.1
31	-	+	+	+	+	-	bode	-36.5	10	25.6	16.1
32	+	+	+	+	+	-	abcde	-39.0	18	22.3	16.1
33	-	-	-	-	-	+	f	-33.2	12.5	23.2	16.2
34	+	-	-	-	-	+	af	-35.6	17	20.1	16.2
35	-	+	-	-	-	+	bf	-36.5	2.5	30.7	16.3
36	+	+	-	-	-	+	abf	-39.0	4.5	27.7	16.5
37	-	-	+	-	-	+	cf	-33.2	6.5	25.9	16.3
38	+	-	+	-	-	+	acf	-35.6	10.5	23.0	16.3
39	-	+	+	-	-	+	bcf	-36.5	5.5	26.4	16.5
40	+	+	+	-	-	+	abcf	-39.0	6.5	23.8	16.6
41	-	-	-	+	-	+	df	-33.2	15	23.0	16.1
42	+	-	-	+	-	+	adf	-35.6	21	19.8	16.1

Appendix B Factorial Design Data

Run Number	Factor A	Factor B	Factor C	Factor D	Factor E	Factor F	Run Label	Open-circuit sensitivity (dB ref. 1 V/Pa)	Bandwidth (kHz)	Mechanical-thermal noise [dB(A)]	C_A/C_M ratio
43	-	+	-	+	-	+	bdf	-36.5	2.5	30.5	16.1
44	+	+	-	+	-	+	abdf	-39.0	5	27.4	16.3
45	-	-	+	+	-	+	cdf	-33.2	7.5	25.7	16.2
46	+	-	+	+	-	+	acdf	-35.6	12.5	22.5	16.2
47	-	+	+	+	-	+	bcdf	-36.5	7	26.0	16.3
48	+	+	+	+	-	+	abcdf	-39.0	9	23.1	16.3
49	-	-	-	-	+	+	ef	-33.2	18	22.8	16.1
50	+	-	-	-	+	+	aef	-35.6	25	19.5	16.1
51	-	+	-	-	+	+	bef	-36.5	2.75	30.4	16.0
52	+	+	-	-	+	+	abef	-39.0	6	27.2	16.2
53	-	-	+	-	+	+	cef	-33.2	9.5	25.5	16.2
54	+	-	+	-	+	+	acef	-35.6	15	22.1	16.2
55	-	+	+	-	+	+	bcef	-36.5	9.5	25.7	16.2
56	+	+	+	-	+	+	abcef	-39.0	12.5	22.5	16.2
57	-	-	-	+	+	+	def	-33.2	19.5	22.8	16.1
58	+	-	-	+	+	+	adef	-35.6	30	19.4	16.1
59	-	+	-	+	+	+	bdef	-36.5	2.5	30.4	15.9
60	+	+	-	+	+	+	abdef	-39.0	6.0	27.1	16.1
61	-	-	+	+	+	+	cdef	-33.2	10.0	25.4	16.1
62	+	-	+	+	+	+	acdef	-35.6	18.0	22.0	16.1
63	-	+	+	+	+	+	bcdef	-36.5	10.0	25.6	16.1
64	+	+	+	+	+	+	abcdef	-39.0	16.0	22.4	16.2

Appendix C

Effect of Backplate Parameters on the Frequency Response Characteristics and Mechanical-thermal Noise of the Condenser Microphone

C.1 Effect of the BC Interaction

As illustrated in Table 3.XII, the BC interaction is significant for both the mechanical-thermal noise and the bandwidth but the main effect of C is relatively insignificant for both cases. Although C is least significant as compared to A and B , it does play an important role in both the mechanical-thermal noise and the bandwidth by virtue of its interaction with B . Figures C.1(a) and C.1(b) illustrate the effect of the BC interaction on the frequency response and the mechanical-thermal noise voltage spectrum density plots, respectively. From Fig. C.1, the $B-C-$ factor combination presents the best performance characteristics – highest sensitivity, widest bandwidth and lowest mechanical-thermal noise – of all four BC factor combinations. With reference to the $B-C-$ factor combination, for the $B-C+$ factor combination, a significant increase in mechanical-thermal noise and a significant reduction in bandwidth are observed while the sensitivity remains unchanged. Thus, when the hole location is shifted further away from the backplate center, the performance deteriorates. Also clearly shown in Fig. C.1, the $B+C-$ factor combination results in the worst performance characteristics with the lowest sensitivity, narrowest bandwidth and highest mechanical-thermal noise. Based on the results of the $B-C-$ and $B-C+$ factor combinations, one would expect that the performance characteristics of the $B+C+$ factor combination will be worse than those of the $B+C-$ factor combination when the

Appendix C Effect of Backplate Parameters

(a)

(b)

Fig. C.1. Effect of the BC interaction on (a) the frequency response and (b) the mechanical-thermal noise voltage spectrum density plots.

hole location is shifted outwards from the backplate center. However, on the contrary, only the reverse situation is true as shown in Fig. C.1. Therefore, the *BC* interaction is an important consideration in the backplate design.

C.2 Effect of the Acoustic Slot

There is an optimum size [1, 2] for the backplate design, which states that the ratio of the backplate radius to the diaphragm radius is 0.82. This optimum size criterion implies an acoustic slot around the circumference of the backplate. From Table 3.IV, the B&K design has a ratio of 0.72 and for comparison purpose, the slot is intentionally removed. Figures C.2(a) and C.2(b) illustrate the effect of the slot on the frequency response and the mechanical-thermal noise voltage spectrum density plots, respectively. For the frequency response plot, with a slot, the bandwidth is significantly larger. For the noise voltage spectrum density plot, with a slot, the mechanical-thermal noise drops significantly. With a slot, the surface area of the backplate is reduced, which leads to a decrease in system damping and thus, a subsequent increase in bandwidth and a decrease in mechanical-thermal noise.

C.3 Effect of the Air Gap Thickness

Figures C.3(a) and C.3(b) illustrate the effect of the air gap thickness on the frequency response and the mechanical-thermal noise voltage spectrum density plots, respectively. With reference to the B&K design of 20 μm (Table 3.IV), for an air gap thickness of 15 μm , a significant increase in microphone sensitivity and mechanical-thermal noise, and a significant decrease in bandwidth are observed. A high value of

Appendix C Effect of Backplate Parameters

(a)

(b)

Fig. C.2. Effect of the acoustic slot on (a) the frequency response and (b) the mechanical-thermal noise voltage spectrum density plots.

Appendix C Effect of Backplate Parameters

(a)

(b)

Fig. C.3. Effect of the air gap thickness on (a) the frequency response and (b) the mechanical-thermal noise voltage spectrum density plots.

mechanical-thermal noise and a low value of bandwidth are not desirable for any microphone design. On the other hand, for an air gap thickness of 25 and 30 μm , a significant decrease in microphone sensitivity is observed. Although there are significant enhancements in both the bandwidth and mechanical-thermal noise, these improvements are at the expense of lower sensitivity. In addition, their frequency response curves show that they are transitioning towards underdamped systems as evidenced by the visible resonant peaks. A narrow air gap results in an increase in system damping, which leads to a lower bandwidth and a higher mechanical-thermal noise. The reverse is also true for a wider air gap. Therefore, the B&K design of 20 μm air gap thickness is a good compromise between sensitivity, bandwidth, and mechanical-thermal noise.

C.4 Effect of the Location of Acoustic Holes

Figures C.4(a) and C.4(b) illustrate the effect of the location of acoustic holes on the frequency response and the mechanical-thermal noise voltage spectrum density plots, respectively. With reference to the B&K design of 0.55 mm (Table 3.IV), at a radius ring of 0.3 and 0.5 mm, there is an observable bandwidth reduction and mechanical-thermal noise increase due to an increase in the damping property. At a radius ring of 0.6 and 0.8 mm, no significant deviations in bandwidth and mechanical-thermal noise are observed. The above results indicate that the location of holes does affect the performance of the microphone and there is an optimum location for the holes in the backplate. For this particular case, the optimum location for the holes is between 0.5 to 0.8 mm radius ring. However, with stricter tolerances at resonant

Appendix C Effect of Backplate Parameters

(a)

(b)

Fig. C.4. Effect of the location of acoustic holes on (a) the frequency response and (b) the mechanical-thermal noise voltage spectrum density plots.

frequency, the optimum location narrows down to between 0.5 and 0.6 mm radius ring, which is in the vicinity of the B&K design of 0.55 mm.

References

- [1] A. J. Zuckerwar, "Principles of operation of condenser microphones," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 3, pp. 37–69.
- [2] M. S. Hawley, F. F. Romanow and J. E. Warren, "The Western Electric 640AA capacitance microphone: Its history and theory of operation," in *AIP Handbook of Condenser Microphone: Theory, Calibration and Measurements*, edited by George S. K. Wong and Tony F. W. Embleton (AIP Press, New York, 1995), Chap. 2, pp. 8–34.

List of Publications

(A) Journal Publications

1. C. W. Tan, K. A. G. Prakash, Z. H. Wang, X. Ji, J. M. Miao, G. Barbastathis and M. Triantafyllou, "Fabrication, packaging and characterization of a piezoelectric pressure sensor for underwater applications," In preparation.
2. F. X. Ke, J. M. Miao and C. W. Tan, "Reduction of squeeze-film damping in an encapsulated RF MEMS switch," *IEEE Microwave and Wireless Components Letters*, To be submitted.
3. Z. H. Wang, J. M. Miao, C. W. Tan and T. Xu, "Fabrication of piezoelectric MEMS devices - From thin film to bulk PZT wafer," *J. Electroceram.*, vol. 24, no. 1, pp. 25–32 (2010).
4. C. H. Chang, C. W. Tan, J. M. Miao and G. Barbastathis, "Self-assembled ferrofluid lithography: Patterning micro and nanostructures by controlling magnetic nanoparticles," *Nanotechnol.*, vol. 20, no. 42, 495301 (2009).
5. C. W. Tan and J. M. Miao, "Modified Škvor/Starr approach in the mechanical-thermal noise analysis of condenser microphone," *J. Acoust. Soc. Am.*, vol. 126, no. 5, pp. 2301–2305 (2009).
6. C. W. Tan and J. M. Miao, "Design optimization of condenser microphone: A design of experiment perspective," *J. Acoust. Soc. Am.*, vol. 125, no. 6, pp. 3641–3649 (2009).
7. C. W. Tan and J. M. Miao, "Optimization of sputtered Cr/Au thin film for diaphragm-based MEMS applications," *Thin Solid Films*, vol. 517, no. 17, pp. 4921–4925 (2009).
8. X. J. Pan, C. W. Tan, J. M. Miao, J. Kasim, Z. X. Shen and E. Q. Xie, "The stress analysis of Si MEMS devices by micro-Raman technique," *Thin Solid Films*, vol. 517, no. 17, pp. 4905–4908 (2009).
9. Z. H. Wang, J. M. Miao and C. W. Tan, "Acoustic transducers with a perforated damping backplate based on PZT/silicon wafer bonding technique," *Sens. Actuators A*, vol. 149, pp. 277–283 (2009).
10. C. W. Tan, Z. H. Wang, J. M. Miao and X. F. Chen, "A study on the viscous damping effect for diaphragm-based acoustic MEMS applications," *J. Micromech. Microeng.*, vol. 17, no. 11, pp. 2253–2263 (2007).

11. P. Dixit, C. W. Tan, L. H. Xu, N. Lin, J. M. Miao, J. H. L. Pang, P. Backus and R. Preisser, "Fabrication and characterization of fine pitch on-chip copper interconnects for advanced wafer level packaging by a high aspect ratio through AZ9260 resist electroplating," *J. Micromech. Microeng.*, vol. 17, no. 5, pp. 1078–1086 (2007).
12. C. W. Tan and J. M. Miao, "Analytical modeling for bulk-micromachined condenser microphone," *J. Acoust. Soc. Am.*, vol. 120, no. 2, pp. 750–761 (2006).
13. C. W. Tan and J. M. Miao, "Enhanced analytical model for micromachined microphone," *J. Phys.: Conf. Ser.*, vol. 34, pp. 847–852 (2006).

(B) Conference Publications

Invited Presentation

1. G. Barbastathis, W. J. Arora, C. H. Chang, M. Deterre, H. J. In, H. Lee, A. J. Nichol, N. S. Shaar, C. S. Tan, C. W. Tan, Z. Wang, S. G. Kim, C. Livermore, J. Miao, Y. Shao-Horn and H. I. Smith, "The Nanostructured Origami™ three-dimensional (3D) fabrication and assembly method," *2nd Intl. Conf. from Nanoparticles & Nanomaterials to Nanodevices & Nanosystems (IC4N 2009)*, Rhodes, Greece, pp. 19, 28 June–3 July 2009.
2. J. M. Miao, C. W. Tan and Z. H. Wang, "Design considerations of silicon microphone backplates" *Proc. 19th International Congress on Acoustics (ICA)*, Madrid, Spain, 2–7 September 2007.
3. J. M. Miao and C. W. Tan, "Two-wafer bulk-micromachined silicon microphone," *Joint Meetings of the Acoustical Society of America and Acoustical Society of Japan*, Honolulu, Hawaii, USA, 28 November–2 December 2006.

Presentation

1. C. W. Tan, K. A. G. Prakash, Z. H. Wang, X. Ji, J. M. Miao, G. Barbastathis and M. Triantafyllou, "Damping characteristics of a micromachined piezoelectric diaphragm-based pressure sensor for underwater applications", *16th International Conference on Solid-state Sensors, Actuators and Microsystems (Transducers' 11)*, Beijing, China, 5–9 June 2011, Under review.
2. C. W. Tan, J. M. Miao, G. Barbastathis and M. Triantafyllou, "A diaphragm-based pressure sensor packaged using liquid crystal polymer and silicone oil for underwater applications", *5th Asia-Pacific Conference on Transducers and Micro-Nano Technology (APCOT 2010)*, Perth, Australia, 6–9 July 2010.

3. C. H. Chang, C. W. Tan, Z. H. Wang, J. M. Miao and G. Barbastathis, "Ferrofluid lithography," *53rd International Conference on Electron, Ion, Photon Beam Technology and Nanofabrication (EIPBN 2009)*, Florida, USA, 26–29 May 2009.
4. F. X. Ke, J. M. Miao and C. W. Tan, "Performance enhancement by substrate perforation for a wafer-level encapsulated RF MEMS DC shunt switch," *Proc. IEEE MEMS 2009 Conference*, Sorrento, Italy, pp. 876–879, 25–29 Jan 2009.
5. C. W. Tan and J. M. Miao, "Optimization of sputtered Cr/Au thin film for diaphragm-based MEMS applications," *Proc. 4th International Conference on Technological Advances of Thin Films & Surface Coatings (ThinFilms 2008)*, Singapore, 13–16 July 2008.
6. X. J. Pan, C. W. Tan, J. M. Miao, J. Kasim, Z. X. Shen and E. Q. Xie, "The stress analysis of Si MEMS devices by micro-Raman technique," *Proc. 4th International Conference on Technological Advances of Thin Films & Surface Coatings (ThinFilms 2008)*, Singapore, 13–16 July 2008.
7. C. W. Tan, Z. H. Wang, J. M. Miao and X. F. Chen, "Consideration of the viscous damping effect in the design and fabrication of a PZT diaphragm-based acoustic device," *Proc. 4th International Conference on Materials for Advanced Technologies (ICMAT 2007)*, Singapore, 1–6 July 2007.
8. C. W. Tan and J. M. Miao, "Enhanced analytical model for micromachined microphone," *Proc. International MEMS Conference (iMEMS 2006)*, Singapore, 9–12 May 2006.