Abstract: Fabrication of microwave slot array antennas and waveguide bandpass and notch filters using 3D printing has significant advantages in terms of speed and cost even for parts with high mechanical complexity. One disadvantage of Stereolithography (SLA) 3D printed, copper plated microwave components is that some SLA resins have a high Coefficient of Thermal Expansion (CTE), quoted in micrometers per meter per degree or $10^{-6}$ per degree. Compared to typically used metals such as aluminum (CTE $24 \times 10^{-6} \cdot K^{-1}$) and copper (CTE $17 \times 10^{-6} \cdot K^{-1}$), SLA resin can have CTE above $100 \times 10^{-6} \cdot K^{-1}$. Resonant structures experience significant frequency drift with temperature changes on the order of $10–50$ °C. The issue of 3D printed microwave structures changing frequency characteristics significantly with temperature shift has not been addressed or reviewed in current literature. We measured and simulated the effect of temperature change on a slot array, cavity notch filters, and post loaded waveguide bandpass filters. We tested several types of SLA resin, different plating techniques, and also Direct Metal Laser Sintering (DMLS) and Binder Infusion metal 3D printing. Performance as a function of temperature is presented for these alternatives.

Keywords: cavity resonators; filters; microwave; plating; stereolithography; thermal expansion; three-dimensional printing

1. Introduction

Microwave slot array antennas and bandpass and notch filters can be produced quickly and inexpensively using 3D printing [1–3]. The high CTE of SLA resins causes frequency drift with temperature to be notably worse than solid metal structures [4]. Somos Taurus SLA resin [5] has CTE of $105.3 \times 10^{-6} \cdot K^{-1}$ over the temperature range 0–50 °C. Choosing lower CTE resin, strengthening metal coatings using multiple layers of nickel and copper, and switching to 3D metal printing with DMLS or binder infusion improves frequency dependence on temperature. In this paper, the frequency dependence with temperature is presented for multiple SLA resins, multiple metal coatings, and 3D metal printed structures. Theoretical fits to expected temperature variation are presented to confirm the predictability of performance. Armed with these predictions and results, requirements for temperature stability can be applied to make decisions regarding the materials and processes to use for 3D printed fabrication of microwave devices.

Another notable advantage of 3D printed parts is that they are much lighter than metal parts. Invar is a 36% nickel alloy with iron. It is used when temperature stability is important. It is ductile and weldable. Its CTE over 20–100 °C is <$1.3 \times 10^{-6} \cdot K^{-1}$, with $\sigma = 0.202 \times 10^6$ S/m (must be plated for use in microwave circuits). Its density is 8.1 to 8.2 g·cm$^{-3}$. Accura Bluestone [6] has solid density of 1.78 g·cm$^{-3}$ at 25 °C, so invar is 4.6 times heavier than Accura Bluestone. Using metals with low CTE [7] is a conventional way to attack the temperature variation problem. There are also various methods of compensating temperature variations in microwave structures [8–19].

It is possible to achieve metal CTE characteristics with 3D printing using DMLS. The AlSi10Mg alloy used for DMLS 3D printing has a CTE of $20.5 \times 10^{-6} \cdot K^{-1}$ [20] and $\sigma = 11.3 \times 10^6$ S/m [21], which is
better CTE than pure aluminum. There are a range of CTE values from $53 \times 10^{-6} \text{K}^{-1}$ to $145 \times 10^{-6} \text{K}^{-1}$ available for a desktop SLA 3D printer [22]. Electroplating can also use layers of copper and nickel to strengthen the metal coating and resist the expansion of plastic. Relatively broad band structures such as slot arrays and bandpass filters are not as badly affected by frequency drift as narrow band notch filters.

2. Materials and Methods

We measured the effect of temperature change on a slot array, cavity notch filters with single and multiple cavities, and post loaded waveguide bandpass filters. We tested several types of SLA resin, different plating techniques, and also DMLS and Binder Infusion metal 3D printed structures.

Analytical predictions of frequency shifts with temperature and calculation of effective CTE are estimates based on bulk scaling of resonators. Analytical scaling and HFSS bulk scaling results are consistent. Mechanical complications of the expansion of the structure causing it to expand in a more complicated way than bulk scaling cannot be simulated analytically or with HFSS. Metal coatings decrease the effect of plastic expansion. Quoted commercial CTE values for 3D printed resin are assumed to be accurate, and their quoted values change with temperature ranges. There is no quoted CTE for the binder infusion metal. The DMLS metal CTE is based on a publication. Measurements present the actual frequency drift performance of each device. Calculations and predictions offer a way to know the impact of material and construction choices.

The oven used for temperature testing was a Test Equity model 107, with temperature range $-42$ to $+130 \degree C$. The network analyzer used in the measurements was a Keysight N5242A. Commercial waveguide to coaxial transitions were used to test both filters and a slot array antenna.

An 18 GHz slot array was cycled in temperature from $-10$ to $+60 \degree C$ six times in 24 h intervals. Only $S_{11}$ was measured. After six cycles, there was visible warping of the structure. The $S_{11}$ minimum changed permanently after each cycle, so there was a hysteresis effect with a 70 °C temperature excursion. Our theory is that the plastic expanding during heating pushes the metal plating, and when the plastic shrinks during cooling, the metal does not return exactly to its original position.

As shown in Figure 1, the minimum $S_{11}$ was $-20.047 \text{ dB}$ at 18.07 GHz on the first cycle, $-33.16 \text{ dB}$ at 18.12 GHz after the second cycle, $-32.11 \text{ dB}$ at 18.13 GHz after the third cycle, $-28.94 \text{ dB}$ at 18.14 GHz after the fourth cycle, $-22.19 \text{ dB}$ at 18.15 GHz after the fifth cycle, and $-27.36 \text{ dB}$ at 18.14 GHz after the sixth cycle.

![Minimum $S_{11}$ Frequency of 18 GHz Slot Array](image)

Figure 1. Minimum $S_{11}$ frequency of slot array temperature cycled $-10 \degree C$ to $+60 \degree C$.
The maximum frequency excursion was 80 MHz, which is a 0.44% permanent change. The \(-10\) dB bandwidth did not change from 100 MHz or 0.55%. A 70 °C temperature shift, a minimum of \(-10\) °C and a maximum of +60 °C cause permanent damage to a 3D printed, copper plated microwave slot array. An obvious alternative that would avoid hysteresis and damage is metal 3D printing. Metal 3D printing with DMLS becomes expensive for part dimensions greater than six inches.

Three slot arrays were simulated with CTE of \(50 \times 10^{-6}\) K\(^{-1}\) to examine the effects of temperature change on gain and \(S_{11}\) parameters. A 315 \(\times\) 323 mm array designed to operate at 18.4 GHz showed an \(S_{11}\) minimum at 18.375 GHz, with \(S_{11} = -30.52\) dB with no temperature offset. The \(-10\) dB \(S_{11}\) width was from 18.3 to 18.44 GHz (140 MHz bandwidth). With a 20-degree temperature offset, the minimum \(S_{11}\) shifted to 18.356 GHz (\(\Delta f = -19\) MHz), with \(S_{11} = -30.78\) dB. The \(-10\) dB \(S_{11}\) width was from 18.28 to 18.43 GHz. With a 50-degree temperature offset, the minimum \(S_{11}\) shifted to 18.330 GHz (\(\Delta f = -45\) MHz), with \(S_{11} = -35.56\) dB. The \(-10\) dB \(S_{11}\) width was from 18.25 to 18.40 GHz. The temperature scaling is shown in Figure 2. This array used a four-point corporate network, so it had a wide bandwidth (145 MHz or 7.9%). The realized gain at the design frequency of 18.4 GHz was 34.16 dB with no temperature offset, and reduced to 33.92 dB with a 20-degree temperature offset and to 33.53 dB with a 50-degree temperature offset (\(-0.62\) dB). This is due to the shift in optimum \(S_{11}\).

![18.4 GHz Slot Array Temperature Scaling](image)

**Figure 2.** 18.4 GHz slot array temperature scaling.

A 301 \(\times\) 319 mm array designed to operate at 10.73 GHz had a minimum \(S_{11}\) at 10.7305 GHz, with \(S_{11} = -20.26\) dB with no temperature offset as shown in Figure 3. The \(-10\) dB \(S_{11}\) width was from 10.69 to 10.76 GHz (70 MHz bandwidth). With a 20-degree temperature offset, the minimum \(S_{11}\) shifted to 10.721 GHz (\(\Delta f = -9.5\) MHz), with \(S_{11} = -20.11\) dB. The \(-10\) dB \(S_{11}\) width was from 10.68 to 10.7530 GHz. With a 50-degree temperature offset, the minimum \(S_{11}\) shifted to 10.705 GHz (\(\Delta f = -25.5\) MHz), with \(S_{11} = -20.87\) dB. The \(-10\) dB \(S_{11}\) width was from 10.67 to 10.74 GHz. This array was coaxial fed in the center of a centered feeding waveguide. The realized gain at the design frequency of 10.73 GHz reduced from 28.42 dB with no temperature offset to 28.45 dB with a 20-degree temperature offset and 28.13 dB with a 50-degree temperature offset (\(-0.29\) dB).
waveguide fed from the end, which is why it has smaller bandwidth. The realized gain at the design with $S_{11} = -30.11$ dB with no temperature offset as shown in Figure 4. The $-10$ dB $S_{11}$ width was from 8.13 to 8.22 GHz (90 MHz bandwidth). With a 20-degree temperature offset, the minimum $S_{11}$ shifted to 8.161 GHz ($\Delta f = -10$ MHz), with $S_{11} = -32.42$ dB. The $-10$ dB $S_{11}$ width was from 8.12 to 8.21 GHz. With a 50-degree temperature offset, the minimum $S_{11}$ shifted to 8.147 GHz ($\Delta f = -24$ MHz), with $S_{11} = -35.4$ dB. The $-10$ dB $S_{11}$ width was from 8.11 to 8.19 GHz. The array used a center feeding waveguide fed from the end, which is why it has smaller bandwidth. The realized gain at the design frequency of 8.18 GHz was 29.84 dB with no temperature offset, reduced to 29.81 dB with a 20 °C offset, and reduced to 29.66 dB with a 50-degree temperature offset ($-0.18$ dB).

A 448 × 420 mm array designed to operate at 8.18 GHz showed an $S_{11}$ minimum at 8.171 GHz, with $S_{11} = -30.11$ dB with no temperature offset as shown in Figure 4. The $-10$ dB $S_{11}$ width was from 8.13 to 8.22 GHz (90 MHz bandwidth). With a 20-degree temperature offset, the minimum $S_{11}$ shifted to 8.161 GHz ($\Delta f = -10$ MHz), with $S_{11} = -32.42$ dB. The $-10$ dB $S_{11}$ width was from 8.12 to 8.21 GHz. With a 50-degree temperature offset, the minimum $S_{11}$ shifted to 8.147 GHz ($\Delta f = -24$ MHz), with $S_{11} = -35.4$ dB. The $-10$ dB $S_{11}$ width was from 8.11 to 8.19 GHz. The array used a center feeding waveguide fed from the end, which is why it has smaller bandwidth. The realized gain at the design frequency of 8.18 GHz was 29.84 dB with no temperature offset, reduced to 29.81 dB with a 20 °C offset, and reduced to 29.66 dB with a 50-degree temperature offset ($-0.18$ dB).

Figure 3. 10.73 GHz slot array temperature scaling.

Figure 4. 8.18 GHz slot array temperature scaling.
Given that all three arrays lose less than one dB of realized gain with a 50 °C temperature increase, slot array performance is apparently not significantly affected by operating at high temperatures. The effect of temperature variation is much less pronounced than it is for narrow band notch filters and to a lesser degree bandpass filters. On the other hand, a high enough temperature will cause permanent changes to the array, and with a high enough temperature, visible damage.

A microwave notch filter can be created by coupling a single or multiple resonant cavities to a waveguide [23]. The cavity resonance causes a dip in $S_{21}$, notching the frequency out of the passband. Not only do the resonant cavity dimensions grow with increasing temperature, but the coupling apertures will also increase in size, which also decreases the resonant frequency assuming that the cavity was originally under-coupled.

The resonant frequency of a TE$_{101}$ rectangular cavity is given by

$$f_{101} = \frac{c}{2\pi \sqrt{\mu \varepsilon}} \sqrt{\left(\frac{\pi}{a}\right)^2 + \left(\frac{\pi}{d}\right)^2} \quad (1)$$

For a resonant frequency of 10 GHz based on WR-90 waveguide $(a = 0.9 \text{ inches} = 22.86 \text{ mm})$, the guide wavelength is given by

$$\lambda_g = \frac{\lambda_0}{\sqrt{1 - \left(\frac{a}{\lambda_c}\right)^2}} \quad (2)$$

where $\lambda_c$ is the cutoff wavelength given by two times the waveguide width ‘a.’ At 10 GHz, $\lambda_0 = 29.979 \text{ mm}$ and $\lambda_g = 39.707 \text{ mm}$.

The length of a TE$_{101}$ cavity is exactly $\lambda_g/2$ so $d = 19.854 \text{ mm}$. As temperature increases, cavity dimensions change according to the following formulas:

$$a = 22.86\text{mm} \times (1 + \text{CTE} \times \Delta T) \quad (3)$$
$$d = 19.854\text{mm} \times (1 + \text{CTE} \times \Delta T) \quad (4)$$

The resonant frequency as a function of temperature and CTE is shown in Figure 5 below.

![Frequency Scaling With Temperature](image)

**Figure 5.** Frequency scaling of 10 GHz TE$_{101}$ Resonant Cavity – Temperature versus CTE.
For a notch filter centered at 10 GHz, the slope for CTE $20 \times 10^{-6}\text{K}^{-1}$ is $-200 \text{kHz}\cdot\text{K}^{-1}$. For CTE of $50 \times 10^{-6}\text{K}^{-1}$, the slope is $-499 \text{kHz}\cdot\text{K}^{-1}$. For CTE of $100 \times 10^{-6}\text{K}^{-1}$, the slope is $-997 \text{kHz}\cdot\text{K}^{-1}$. Equation (1) combined with Equations (3) and (4) yields the approximate result that the slope of resonant frequency change with temperature is $-\text{CTE}$ multiplied by the unmodified resonant frequency. This matches the fit slopes for CTE 20, 50, and $100 \times 10^{-6}\text{K}^{-1}$.

For a TE$_{101}$ notch filter centered at 15.7 GHz, using the same analytical scaling, CTE of $20 \times 10^{-6}\text{K}^{-1}$ produces a slope of $-300 \text{kHz}\cdot\text{K}^{-1}$, a CTE of $50 \times 10^{-6}\text{K}^{-1}$ produces a slope of $-800 \text{kHz}\cdot\text{K}^{-1}$, and a CTE of $100 \times 10^{-6}\text{K}^{-1}$ produces a slope of $-1.6 \text{MHz}\cdot\text{K}^{-1}$. For a DMLS 3D printed cavity filter made with AlSi10Mg, with a notch at 15.644 GHz, with CTE published as $20.5 \times 10^{-6}\text{K}^{-1}$, we measured $-16 \text{MHz}$ shift for a temperature range from 23 to 55 °C, corresponding to $-500 \text{kHz}\cdot\text{K}^{-1}$. Measurement of $S_{21}$ at 20, 23, 35, 40, 45, and 50 °C was followed by plotting the $S_{21}$ null versus temperature and applying a linear fit to the data. An analytical fit to this frequency change corresponds to a CTE of $32 \times 10^{-6}\text{K}^{-1}$. An image of the single cavity DMLS metal 3D printed notch filter is shown in Figure 6.

![Figure 6. DMLS metal 3D printed single cavity notch filter.](image-url)

A single cavity filter SLA 3D printed with Formlabs Tough resin at 15.9 GHz using WR-62 waveguide, and plated with 12.7 µm copper, 25.4 µm nickel, and 12.7 µm copper measured 15.933 GHz with $S_{21} = -16.04 \text{dB}$ at 20 °C, 15.91 GHz with $S_{21} = -16.26 \text{dB}$ at 35 °C, 15.9036 GHz with $S_{21} = -15.48 \text{dB}$ at 40 °C, and 15.913 GHz with $S_{21} = -28.97 \text{dB}$ at 45 °C. The linear slope fit to these data is $-856.23 \text{kHz}$ per °C. The analytical fit to CTE for this resonant cavity is $53.95 \times 10^{-6}\text{K}^{-1}$. The published CTE of Formlabs Tough resin [24] in the cured state is $119 \times 10^{-6}\text{K}^{-1}$, so the stronger metal coating did have an effect. The heated plastic expands, but a stronger metal coating more strongly resists this expansion. In the limit where the metal coating was very thick, it is obvious that the plastic would be completely prevented from expanding. Returning to 20 °C, the $S_{21}$ null was $-29.89 \text{dB}$ ($-13.85 \text{dB}$ change) at 15.939 GHz ($\Delta f = +6 \text{MHz}$). There was some hysteresis effect.

Images of SLA 3D printed, copper plated single cavity notch filters using WR-42 and WR-62 waveguide are shown in Figure 7.
A single cavity filter SLA 3D printed with Taurus resin at 17.1 GHz using WR-62 waveguide, and plated with copper measured an $S_{21}$ minimum at 17.068 GHz with $S_{21} = -9$ dB at 20 °C, 17.037 GHz with $S_{21} = -8.15$ dB at 35 °C, 17.027 GHz with $S_{21} = -8.31$ dB at 40 °C, 17.015 GHz with $S_{21} = -6.52$ dB at 45 °C, and 17.001 GHz with $S_{21} = -7.61$ dB at 50 °C. As shown in Figure 8, the linear slope fit to these data is $y = -2197.9x + 2E+07$. The analytical fit to CTE for this resonant cavity is $129 \times 10^{-6} \cdot K^{-1}$. The published CTE of Somos Taurus is $105.3 \times 10^{-6} \cdot K^{-1}$. Returning to 20 °C, the $S_{21}$ null was $-7.61$ dB ($+1.39$ dB change) at 17.055 GHz ($\Delta f = -13$ MHz). There was some hysteresis effect.

A three-cavity filter at 17.2 GHz was fabricated using binder infusion metal 3D printing. In this process, a binder is printed on tungsten powder and infused with bronze [25]. The percentage of tungsten is 50–55%. For a TE$_{101}$ notch filter centered at 17.2 GHz, using the same analytical scaling, CTE of $20 \times 10^{-6} \cdot K^{-1}$ produces a slope of $-343$ kHz·K$^{-1}$, a CTE of $50 \times 10^{-6} \cdot K^{-1}$ produces a slope of $-859$ kHz·K$^{-1}$, and a CTE of $100 \times 10^{-6} \cdot K^{-1}$ produces a slope of $-1.71$ MHz·K$^{-1}$. The binder infusion three cavity notch filter measured a frequency shift of 3 MHz over a temperature range from 23 to 50 °C, corresponding to $-111.11$ kHz·K$^{-1}$. Compared to analytical scaling, this corresponds to an effective CTE of $6.5 \times 10^{-6} \cdot K^{-1}$. Since bronze has CTE of $17 \times 10^{-6} \cdot K^{-1}$ and tungsten has CTE of $4.5 \times 10^{-6} \cdot K^{-1}$,

Figure 7. SLA 3D printed single cavity notch filter using WR-42 and WR-62 waveguide.

Figure 8. Somos Taurus SLA 3D printed copper plated notch filter.
assuming 50% of each one should expect overall CTE of $10.75 \times 10^{-6} \, \text{K}^{-1}$. An image of the binder infusion metal 3D printed notch filter is shown in Figure 9.

![Figure 9. Binder infusion metal 3D printed cavity notch filter.](image)

One remedy for the temperature drift problem is to adjust the coupling aperture of a single cavity filter to be near critical coupling. This increases the resonant bandwidth. If the notch width is 150 MHz and the interfering signal has a bandwidth of 50 MHz or less, significant temperature drift can be tolerated while still attenuating the target signal. One problem with this technique is that it does not allow filtering of an interfering signal that is closer in frequency than the notch bandwidth.

The bandpass filter design that was 3D printed is a post loaded waveguide. The inductive posts form coupled resonant cavities. This filter was designed based on a technique described in [24]. The filter has openings along the center of the waveguide broad wall, following the technique described in [1] to enable internal electroplating of a 3D printed structure.

The SLA 3D printed, copper electroplated bandpass filter measured a passband shift of $-549 \, \text{kHz} \cdot \text{K}^{-1}$. The DMLS 3D printed bandpass filter measured a passband shift of $-243.4 \, \text{kHz} \cdot \text{K}^{-1}$. This was determined by measuring the $S_{21} = -15 \, \text{dB}$ point of each bandpass $S_{21}$ characteristic plot as a function of frequency. These were measured at 20, 23, 35, 40, 45, and 50 °C with frequencies 12.6892 GHz (20 °C), 12.6883 GHz (23 °C), 12.6850 GHz (35 °C), 12.6842 GHz (40 °C), 12.6833 GHz (45 °C), and 12.6817 GHz (50 °C) as shown in Figure 10. The value of $f$ for $S_{21} = -15 \, \text{dB}$ at each temperature was plotted and a linear fit was used to characterize the overall temperature variation. The high temp SLA, copper plated bandpass filter measured a passband shift of $-365.89 \, \text{kHz} \cdot \text{K}^{-1}$. The measured frequencies were 12.6742 GHz (20 °C), 12.6733 GHz (23 °C), 12.6708 GHz (35 °C), 12.6658 GHz (40 °C), 12.6650 GHz (45 °C), and 12.6642 GHz (50 °C). Images of the SLA and DMLS 3D printed, post-loaded, waveguide microwave bandpass filters are shown in Figure 11.
Simulation with HFSS using bulk scaling of the structure corresponding to CTE of $20 \times 10^{-6}\cdot K^{-1}$ showed a frequency shift of $-300$ kHz·K$^{-1}$, very close to the measured value for DMLS with actual reported CTE of $20 \times 10^{-6}\cdot K^{-1}$. The HFSS simulation result is shown in Figure 12 for $\Delta T = 0$ to 50°C.
3. Conclusions

Resins used for stereolithography 3D printing typically have high CTE. Microwave structures manufactured with 3D printing using SLA and copper plated can have strong frequency dependence on temperature. In particular, multi-cavity filters with narrow bandwidth are strongly affected by dependence of resonant frequency on temperature. This can preclude their usefulness if the frequency drift approaches the bandwidth of the device. Electroplating with layers of copper and nickel to strengthen the metal coating resists the expansion of plastic and reduces temperature dependence. Using DMLS metal printing approaches metal structure performance, so that metal 3D printed structures can approach temperature characteristics of solid metal structures. Single cavity notch filters printed with lower CTE resin can be useful if a wide enough notch bandwidth is used to accommodate frequency drift. One must choose the right 3D printing materials and processes to achieve the temperature stability required by the application.

Funding: This research received no external funding.

Conflicts of Interest: The authors declare no conflict of interest.

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